The WELDING JOURNAL

DECEMBER 1952

OFFICIAL PUBLICATION OF THE AMERICAN WELDING SOCIETY

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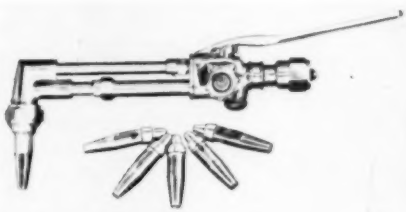

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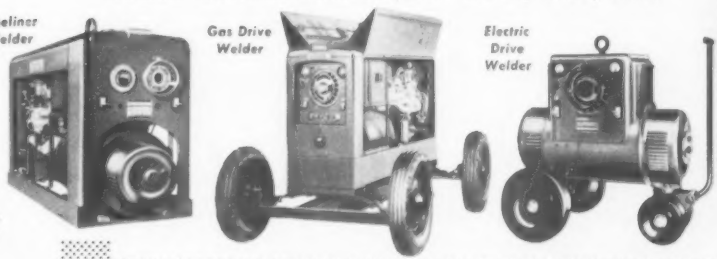

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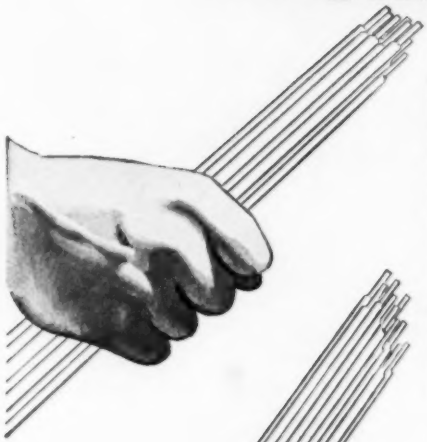

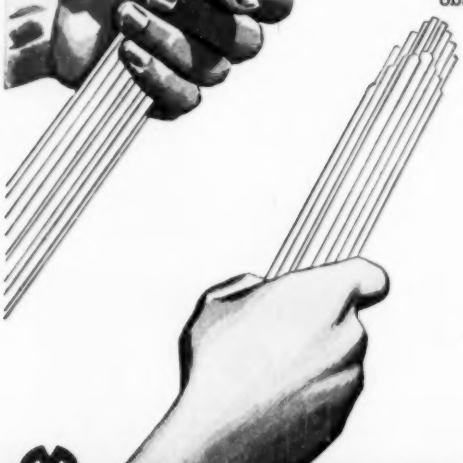

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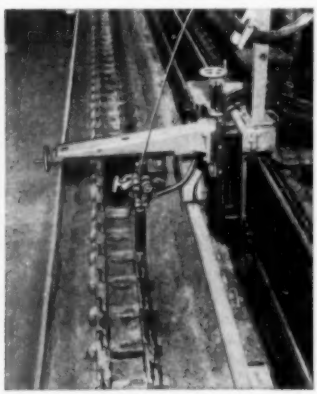
LINDE'S News of Metalworking

Worn Parts Rebuilt Economically by UNIONMELT Welding

Repair of worn tractor parts is most practical and economical with specially developed fixtures on which standard UNIONMELT welding units are mounted. The metal deposited by UNIONMELT welding is so smooth that no grinding or finishing is required. Experience indicates that resurfaced areas will wear as long as new parts.

The Berkeley "Conservall" fixture is designed especially for rebuilding crawler tractor track links or rails without disassembling them. The track is placed in a trough and clamped in position. A LINDE side-beam carriage



Tractor rail links rebuilt in "Conservall" machine by UNIONMELT *welding. Cams automatically control welding action for each link.*

moves the UNIONMELT welding equipment over the positioned track and the welding of each link is automatically started, stopped, and accurately controlled. Rollers, idlers, sheaves, and other circular work can also be welded or resurfaced on another section of the machine.

The "Leader" machine is also available for rebuilding both cylindrical and flat parts. Rollers are rebuilt on top of the machine. Larger parts, such as idlers, are mounted in the chuck at the side of the machine as shown. With the flat work attachment, parts such as disassembled track rail links, grousers, bulldozer blades, end bits and fabricated members can be resurfaced easily. This attachment is operated by a gear which is mounted in the chuck.

These tractor rollers and an idler were rebuilt at a speed of 30 in. per min. by UNIONMELT *welding.*

With both machines, the wheels can be tilted for rebuilding the flanges. It takes only 80 to 90 minutes to rebuild a D-8 track roller. Idlers take about three times as long.

OXWELD 1928 rod is normally used in making these repairs. When wear is excessive, OXWELD 296 rod is sometimes used for the initial buildup which is then finished with OXWELD 1928 rod. For such resurfacing, use either Grade 80 or Grade 90 UNIONMELT welding compositions.

For some services, a finishing pass with a higher alloy tube rod is applied to produce a harder surface. While material of almost any hardness can be applied, one combining hardness and toughness lasts longer than one of higher hardness that tends to spall and chip. As deposited, OXWELD 1928 material has a hardness of about Rockwell C-25, but in service the working surfaces actually develop properties which cause them to outwear deposits that are substantially harder.

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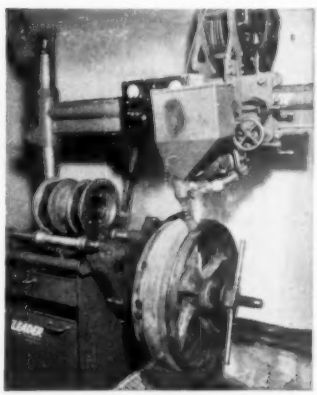
LINDE's engineers and technicians will be glad to give further information about UNIONMELT welding. Telephone or write today.



UNIONMELT *welding head on "Leader" fixture rebuilds rollers and idlers. Rollers are welded on top of the machine, idlers at the side, and flat parts, depending on their size, can be welded on top of the machine or in the flat work attachment.*

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The WELDING JOURNAL

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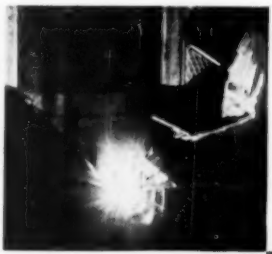
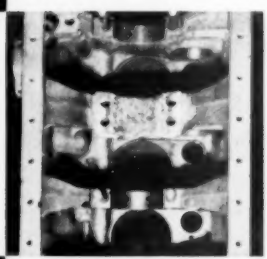
Don't scrap that crankcase

New Welding Technique
...salvages cast iron crank cases with scored bores

Scored or pinched bores used to mean the scrap pile for cast iron crank cases. But that's not so in service departments of Autocar Company branches since they installed a new salvage-by-welding technique. Here's how it's done —

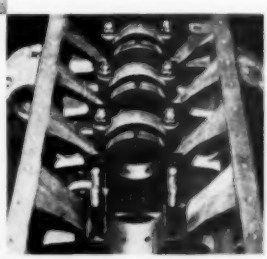


1 Welder deposits a layer of Ni-Rod "55" in center brace bore, while crank case bolted to block rests in upside-down position. This crank case bore hole had previously been machined down about .045" to remove scored area and eliminate oil-impregnated cast iron material. Fast work prevents store-up of excessive heat in cast iron brace.



2 Finished weld deposit has one-bead thickness. Using a ⅛" dia. Ni-Rod "55" electrode, the welder first deposited metal around the oil holes, then laid beads lengthwise in the bore, working from center outward and on alternating sides.

3 Now for the machining: The bearing cap is placed in a fixture. The joint surfaces are ground smooth and in parallel, then securely fastened in place. A special boring bar (bar held in perfect alignment with the other bores) and Carboloy-tipped cutting tool machine Ni-Rod "55" deposit down to the original dimensions. (Tolerance is held to 0.0005 in.)



4 As good as new. After machining, the bore's smooth new surface of weld metal is ready to receive the new bearing. Then the crank case is ready to be assembled. The first case salvaged this way has been in continuous service for over two years now, giving perfect service.

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The WELDING JOURNAL

volume 31 December 1952 number 12

Failures and Defects Encountered in the Construction of Welded Ships

Different types of cracks are classified as to origin and cause with special emphasis on welding procedures, fabrication, workmanship, design and materials

by R. D. Bradway

SINCE the early days of World War II, structural failures in our welded ships in service have demanded our attention and have been a matter of concern. The recent failures of the T2 Tankers, *Pendleton* and *Fort Mercer*, have again focused our attention on the necessity of eliminating these failures in service. Much has been done and is being done both to find the underlying causes of these failures and to prevent their occurrence.

Structural failures are not, however, confined to ships in service. Unfortunately, they also occur during construction. Although these construction failures do not represent the potential hazard in the loss of life or property that service failures do, they should not and cannot be lightly dismissed. Every construction failure or defect of any kind, if undetected or unrepaired, may become the start of a service failure. However, if these failures and defects are properly repaired, we can disregard them as a source of trouble in service, but every welded repair represents a needless amount of extra work and expense and a disruption of the orderly completion of work. The delay involved can be of more concern than the actual cost since these failures too often occur at the most inopportune times. Moreover, both the expense and the delay are detrimental to the best interests of the shipbuilder and represent a needless burden.

The ideal goal for which we all strive is, of course, the reduction of these construction failures and their required repairs to the absolute minimum, for unquestionably the cheapest job is the one that is done right the first time. We can well afford to spend a little additional time and effort to avoid trouble.

It is now a requirement of various inspection agencies and owners that in addition to making the design conform to the Rules and Specifications, a general overall procedure or plan of construction, including the method for a random inspection of important welded joints, be submitted prior to commencing work. The degree and extent of this control will, of course, vary from yard to yard. But, in all cases, it is an attempt to obtain satisfactory ship in the easiest and best manner. It must be realized that every problem that will arise cannot be foreseen and must be treated as it arises in the light of conditions existing at the time.

I like to compare our efforts to build ships without failures and trouble to driving a car. Our procedures set up rules and methods for working, including certain restrictions and precautions. These compare to our traffic laws with their stop signs and traffic signals placed where needed, with one occasionally placed where we can see no earthly reason for it. All of us get in a rush occasionally and slip through a "stop sign," take a chance on a "yellow light" or maybe even run a "red light." Most of the time we do this without incident, but sooner or later someone gets into an accident because he took the chance. Similarly, in the hustle of production, we cut a corner here and take a chance there, usually without incident, but occasionally we are not so lucky and a failure will result.

The basic causes for these structural failures during construction may be classed in the same three groups as were the causes for service failures, namely, Workmanship, Material and Design.

Before discussing these basic causes and their particular part in construction failures, I would like to

R. D. Bradway is Welding Engineer with the New York Shipbuilding Corp., Camden, N. J.

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point out the difference between the causes of service failures and the causes of construction failures. The final report of the Board of Investigation to Inquire into the Design and Methods of Construction of Welded Steel Merchant Ships, convened by the Secretary of the Navy in 1943, stated that in all cases of service failures a definite notch or geometric discontinuity, resulting from either design or workmanship, was determined to be the starting point. This is not always true in the case of construction failures where cracks have occurred under certain conditions of restraint when no notch has been present, particularly in alloy steels.

Although more service fractures started at notches which were the result of design rather than workmanship, the opposite is true of construction failures. Many design notches do not appear until construction is completed, while workmanship can, during construction, introduce many notches or potential notches which are temporary in nature and will disappear as construction continues.

Residual welding stresses were not found to contribute in any great degree to service failures. During construction, however, accumulated stresses from welding are a major cause of cracking.

Service fractures occurred more frequently at lower temperatures because of the effect of low temperatures on the properties of the steel. During construction, failures are more apt to occur with sudden drops of temperature and the resulting increase or change of stress therefrom. March and April, with their warm days and cool nights, are more to be feared than the cold winter months.

In considering the basic causes of construction failures, let us first look at the most important one, Workmanship. Good workmanship, which is necessary for the elimination or reduction of failures, starts with proper planning which must include extent of subassembly, sequence of erection and welding, and the use of proper methods and equipment by capable workmen. Good design will take into consideration the construction methods and procedures to be used and will make both the planning and the work much simpler, which, of course, will help reduce the chances of construction failures.

A good quality of work is necessary to avoid the formation of notches. This includes the work of all crafts because the work of the fitter, burner, chipper, machinist or whoever it may be preparing the work is just as important if not more important than the actual welding. In fact, successful welding is due about 25% to the welder and 75% to the proper preparation. If we could only be sure that our welders would refuse to weld any joint until it was properly prepared, we would have far less cause to be concerned about the formation of notches.

However, sound welding alone does not insure freedom from notches. A notch or discontinuity is formed whenever a weld is not completed either for its full depth or extent. Unfortunately, in normal production where a large share of the work is done on one shift, it is impractical, yes, even impossible, not to leave some unfinished welds. Our best hope is that the work will be so planned that as few unfinished welds as possible will be left, particularly in areas where high stresses may be present and that certain of the more important welds will be worked continuously until completed.

In these days of alloy steel there is still another problem. These materials are successfully welded provided the proper electrode and procedure are used. This means that the various materials must be clearly marked from the time they are received until final completion of the ship. Then, of course, the workmen must recognize these materials and use the proper methods and procedures.

On many of these alloy steels, preheat must be used for successful welding. However, heating is not a general cure-all and must be used judiciously as it can work against as well as for you. Improper heating, with the expansion and subsequent contraction, can create enough stress to cause failure, as can flame straightening with its heating, followed by the water-spray quench.

WELDING PROCEDURES

The part that workmanship plays in construction failures can best be shown by a few typical failures.

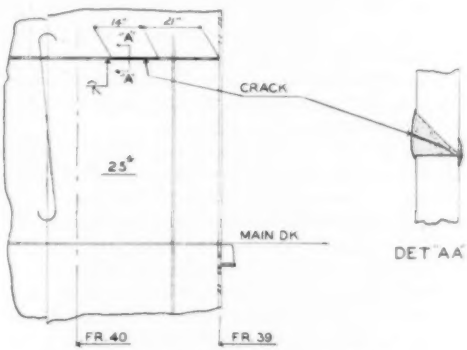

Fig. 1 Cracked butt weld in trunk side

Figure 1: The cracked weld in Fig. 1 was the result of poor penetration and poor fusion of the weld on the first side with inadequate back chipping. This type of cracking is often found on the single-bevel weld shown which, because of its straight side, is difficult to weld satisfactorily. Fortunately, this type of weld preparation is rapidly disappearing but cracks of this kind can also be found in vee welds if the joint is not properly prepared with the correct root opening and bevels. The crack was chipped out and the joint rewelded.

Figure 2: The cracked weld shown in Fig. 2 was the result of improper back chipping The corner of the heavier plate should have been removed to give access

to the root of the weld. The repair was by chipping out the crack and rewelding.

Figure 3: The poor fit of the longitudinal to the wiring tube, together with an improperly made fillet weld, cracked the web of the longitudinal as shown. A new section of longitudinal was added to make the repair.

Figure 4: The cracked fillet weld shown here resulted from the poor fit and failure to make the proper oversize fillet. The repair was made by chipping out the cracked fillet weld and beveling the end of the plate for a full penetration weld. The importance of good fitting and preparation for fillet welds as well as for groove welds cannot be over emphasized. Too often, the weld is not properly oversized to make up for the poor fit and, if it is, the cost of that weld is doubled or tripled.

Figure 5: The crack in the vertical keel butt shown in Fig. 5 was caused by leaving the partially completed butt, welded on one side and only partially block welded on the second side, overnight with a sudden drop in the temperature in addition to having an excessive restraint caused by improperly placed, oversized

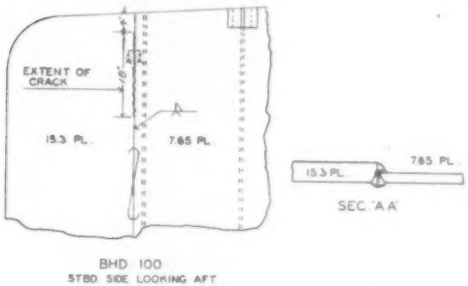

Fig. 2 Cracked butt weld in transbulkhead

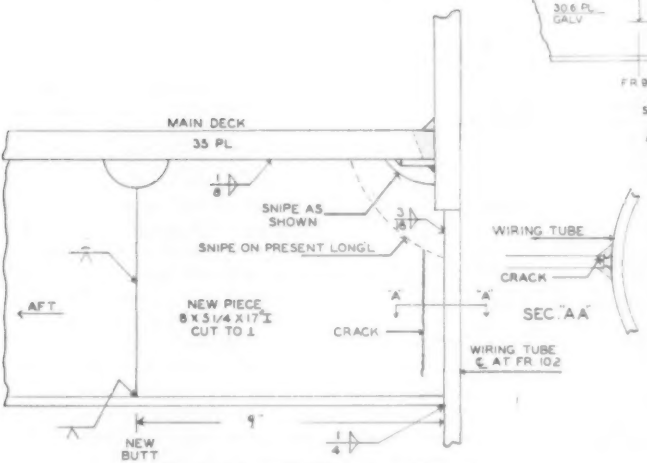

Fig. 3 Longitudinal No. 1 cracked web

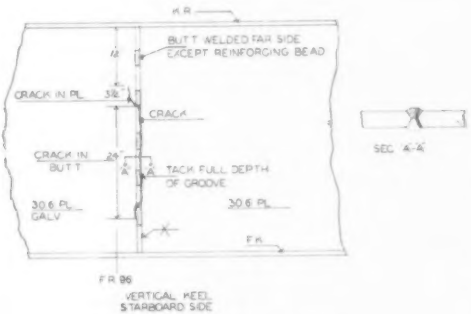

Fig. 5 Cracked butt weld

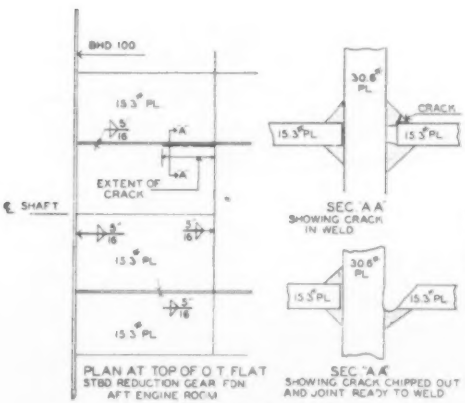

Fig. 4 Cracked fillet weld

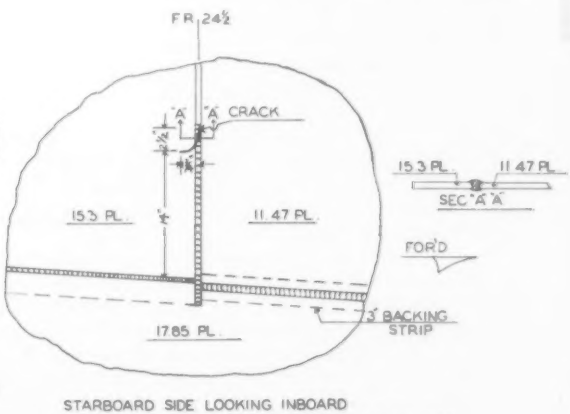

Fig. 6 Crack in shell plate and butt weld

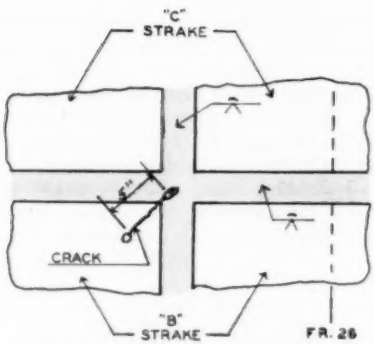

Fig. 7 Cracked shell plate from slag inclusion at weld intersection

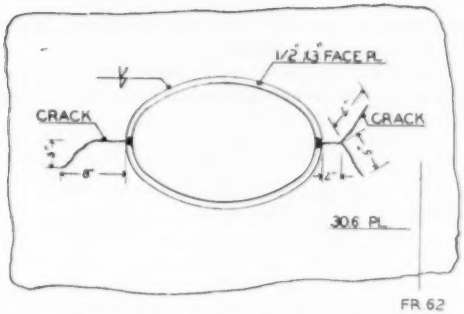

Fig. 10 Crack in longitudinal starting from incomplete butt of face plate

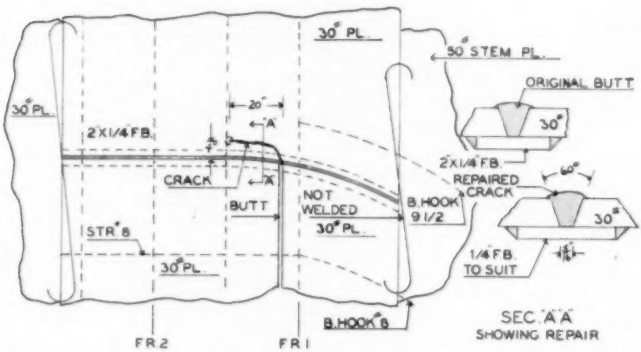

Fig. 8 Crack in shell plate. Shell plating stbd. looking inboard

the crack and welding, taking care to peen the weld. Finally, the hole was countersunk and welded full depth into a backing plate. The weld reinforcement was flushed off and the normal weld reinforcement added.

Figure 8: The crack in the plate was caused by improper welding sequence in which the longitudinal seam weld had been carried beyond an unwelded butt. The stresses from the butt welding started the crack in the seam from which it propagated into the plate. The crack was chipped to give a vee preparation with a backing strip after first drilling a hole at the end of the crack. The repair was welded, peening each pass of the weld.

Figure 9: The crack in the deck plate started from the end of the fillet weld connection to the shell as stress in the deck increased due to other welding on the deck in that vicinity. The crack was chipped out and welded after first chipping back the fillet welds for 12 in.

Figure 10: The cracks in the longitudinal were the

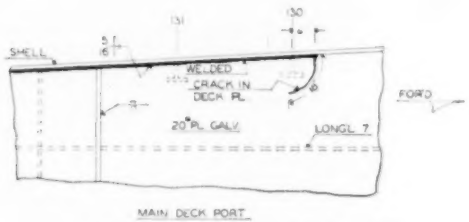

Fig. 9 Crack in main deck plate

tacks to adjacent structure. After chipping out the crack in the plate and in the weld and removing the tacks, the cracks in the plate was first welded and then the butt rewelded in the normal manner.

Figure 6: The crack in the weld and plate in Fig. 6 was the result of improper sequence in leaving a weld uncompleted for its entire length. The crack was chipped out and welded prior to completing the butt weld.

Figure 7: This crack into the plate started at a slag inclusion in the weld intersection. A hole was drilled at the end of the crack to prevent further propagation of the crack. Then repair was made by chipping out

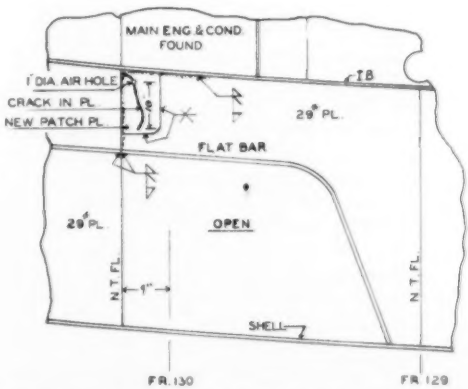

Fig. 11 Crack from notch introduced in restricted corner

result of stress concentration at the unchipped root of the butt in the face plate ring with a sudden drop of temperature overnight. The butts in the ring and the cracks in the longitudinal were chipped out and then welded after first heating the area of the longitudinal at the end of the welds.

Figure 11: The crack in Fig. 11 started from the poor penetration and resulting unsound weld in the restricted corner. Sufficient stress was present to carry the crack through the 1-in. diam. air hole. Repair was made by cutting out the cracked area and inserting a patch plate.

Figure 12: The crack in the engine girder started at the partially completed weld in the face plate. The

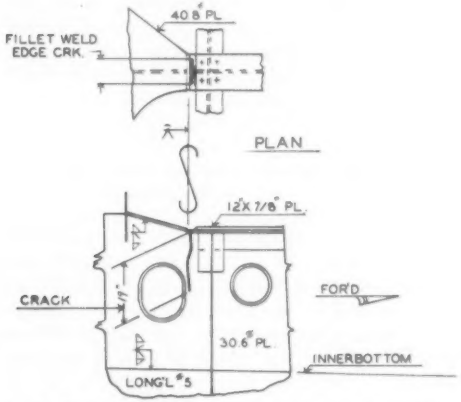

Fig. 12 Port outboard girder. Crack in engine girder starting from partial completed weld in face plate

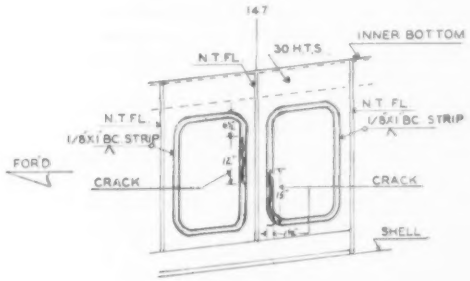

Fig. 15 Cracked welds in flush patch plates.

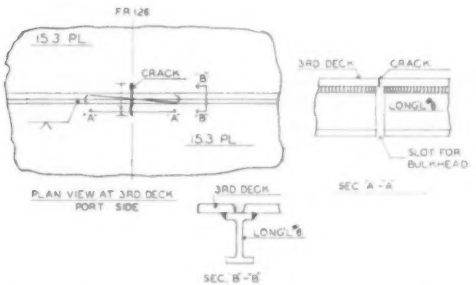

Fig. 13 Cracked during subassembly through slot in longitudinal for fitting bulkhead

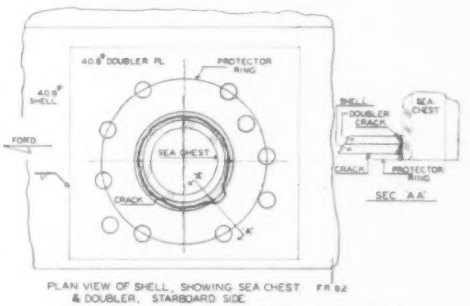

Fig. 16 Cracked sea chest shell connection

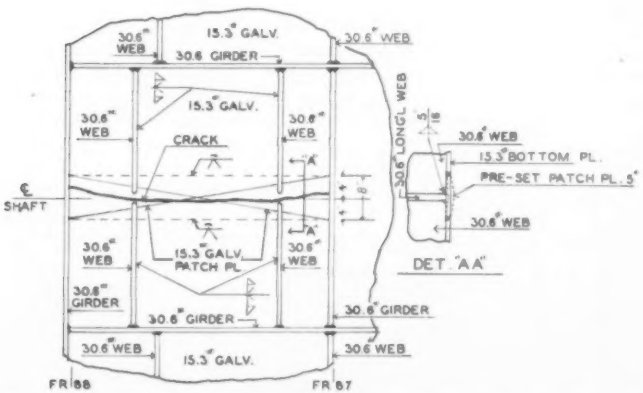

Fig. 14 Plan view of oiltight flat port-side crack through oiltight flat in slot left between webs for fitting longitudinal web

butt in the face plate and the crack in the girder were chipped out and then welded, peening the weld carefully.

Figure 13: The crack in the deck occurred during subassembly, in the notch caused by the slot in the longitudinal that had been left for fitting a bulkhead. The crack was chipped out and welded after first heating the plate area at the ends of the weld.

Figure 14: Figure 14 shows a similar crack in an oiltight flat through the slot left between the webs for fitting a longitudinal web. But in this case a better repair was made by cutting out the

cracked area of the plate and welding in a preset patch plate.

Figure 15: These cracks are typical of cracks that occur only too often from the restraint and resultant high stress of flush patch plates. Repair can be made by chipping out the crack and rewelding, taking care to peen the weld.

Figure 16: This sea chest shell connection is another example of highly restrained weld in which slag inclusions at the root of the weld were sufficient to start cracking. This was repaired by chipping out the weld, preheating the area of the shell around the sea chest and welding, using a block sequence and peening each layer of weld.

Figure 17: The cracked weld connecting the grating to the deck is a typical underbead crack in the hardened zone of the base material. This type of crack, which most often is caused by high carbon in the base material, was in this case caused by the extra stress on the weld due to improper heating. The grating was preheated, rather than the deck, so that in cooling the resulting shrinkage raised the stress on the weld. This was repaired by removing the weld in way of the crack, preheating the deck around the grating with strip heaters so that upon cooling the shrinkage would tend to relieve the stress from welding and then welding, using a block sequence in increments 6 ft 8 in. long.

Figure 18: The crack in the innerbottom plating started at a point of poor fusion at the corner of the beam in way of the flush collar plate which presented an extremely poor detail for obtaining a sound weld. The cracked area of the plate was cut out and a 10-in. repair patch inserted.

Figure 19: This crack in a transverse tanker bulkhead was caused by undercut at the toe of the weld connecting the longitudinal through bracket to the bulkhead. Holes were drilled at the end of the crack to prevent further propagation of the crack and the crack chipped out with sufficient root opening to permit welding into the doubler fitted to the bulkhead. The repair was made peening each layer except the last.

Figure 20: The crack in the cross-head weldment occurred in the base material approximately $\frac{1}{2}$ in. away from the original weld and was attributed to improper preheat to prevent formation of a heat-affected zone in the hardenable base material. The crack was removed by matching a U groove to the depth of the crack. This repair weld was made in the same manner as the original weld except using the proper preheat.

GENERAL FABRICATION PROCEDURES

So far we have only seen failure in which welding or work directly in connection with welding has been the contributing cause for failure. Our picture would not be completed without including other failures that originated from defective workmanship in which welding played no part.

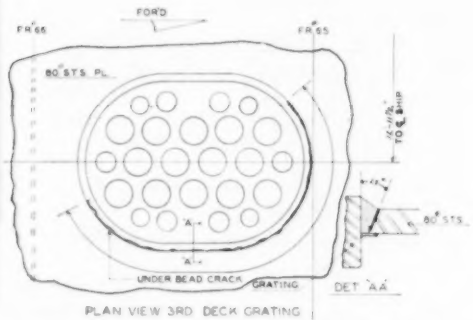

Fig. 17 Cracked weld connecting grating to 3rd deck

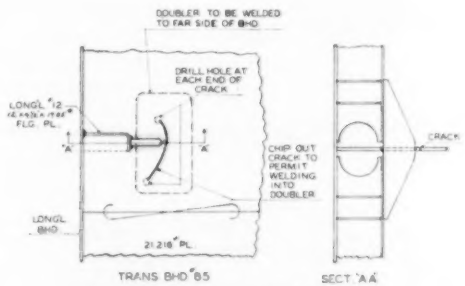
DOUBLER TO BE WELDED TO FAR SIDE OF BHD
DRILL HOLE AT EACH END OF CRACK
CHIP OUT CRACK TO PERMIT WELDING INTO DOUBLER
CRACK
LONGL BHD
21.216" PL
TRANS BHD 85
SECT 'A-A'

Fig. 19 Crack in tanker bulkhead

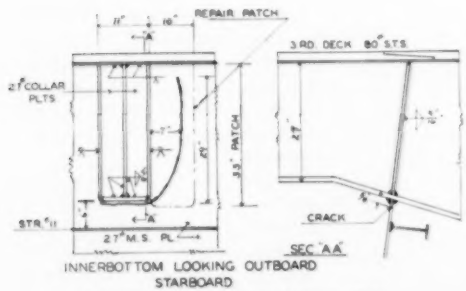

Fig. 18 Crack in inner bottom plate

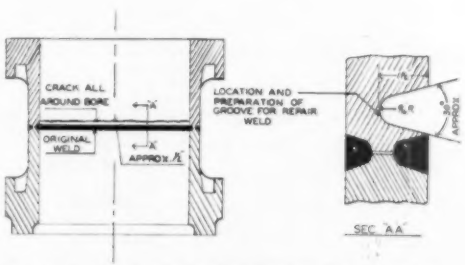

Fig. 20 Base metal crack in rudder crosshead casting

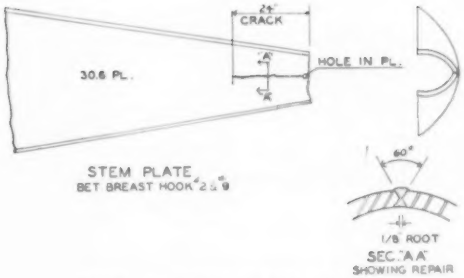

Fig. 21 Stem plate cracked in forming

Figure 21: The crack in this furnaced stem plate developed during the forming process and started from the punched hole which had been used for handling purposes. After furnacing to final shape, the crack was chipped out and welded, using the double-vee preparation shown. The weld was flushed off on the outside for good appearance.

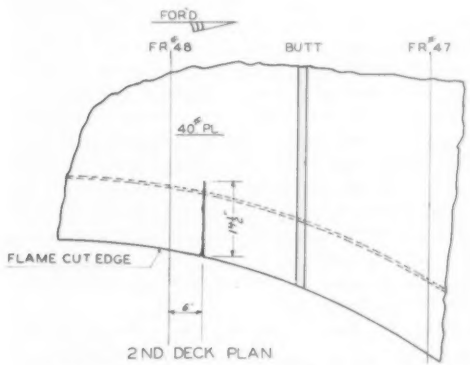

Fig. 22 Crack from notch in flame-cut edge

Figure 22: This crack in the deck plate started at a notch in the flame-cut edge of the circular opening. This was the final trimming cut on the opening after all welding of the deck seams and butts in that vicinity and all welding to the understructure had been completed. The stress from this welding was sufficient to start the crack as soon as the notch was formed. The crack was repaired by chipping out for welding, using a backing strip so that all welding was done from the top side.

Figure 23: This crack started at the sharp corner of the flame-cut weld preparation when subjected to a sudden drop in temperature while the job was left in this condition over a week end. The design should have shown a well-rounded preparation at the end of this weld connection and more care should have been taken in the flame-cut preparation. The repair was made by chipping out and welding using a temporary

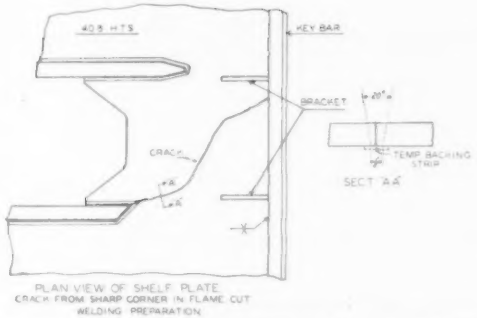

Fig. 23 Crack from sharp corner in flame-cut welding preparation

backing strip. Welding was done using a block sequence, peening each layer of the weld. The weld was flushed top and bottom after completion.

Figure 24: These cracks started at the notch formed by the re-entrant cuts in the bracket and were caused by the heavy fillets which stopped just short of this notch. The fillet weld were cut back for 6 in. from the ends of the face plate and were left unwelded for welding on the ship. The crack was chipped out and then welded.

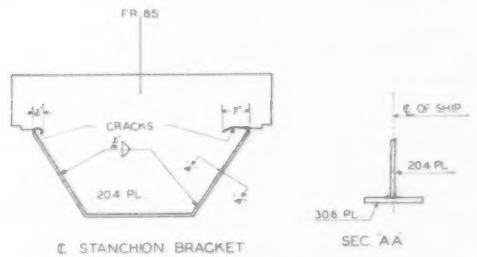

Fig. 24 Crack from square-cut corner

Figure 25: The crack in this forged ring started in the extremely sharp notch at the square corner formed in rough machining. Material in way of the crack was hogged out on the boring mill making a uniform groove preparation for the complete circumference of the ring. The groove was welded using four welders, at quadrant points, with a block sequence peening each pass with a heavy riveting hammer. After welding and prior to final machining the ring was stress relieved.

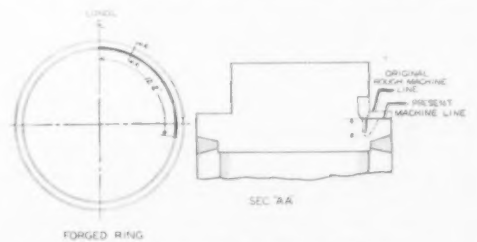

Fig. 25 Crack from rough machined square corner

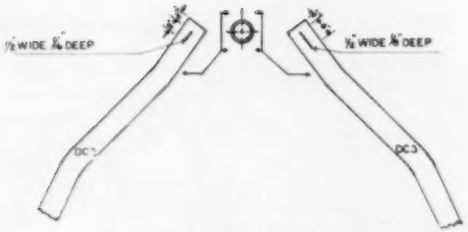

Fig. 26 Gouged boiler tube

Figure 26: Gouges were dug into the surface of this boiler tube in handling. The gouges were ground out the tube preheated and the grooves welded. The entire tube was stress relieved. This repair saved the long delay that would have been required to obtain a replacement tube.

Figure 27: The gouge shown on the edge of the shell plate resulted from an arc burn when a 440-v power cable shorted on the plate edge. This gouged area was built up with welded metal against a temporary plate backing. The surface was flushed down to normal plate thickness after welding.

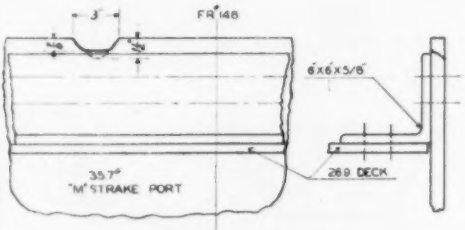

Fig. 27 Gouge in shell plate; 440-v arc burn

Fortunately, such failures as we have just seen are not an everyday occurrence, but every error or omission in workmanship is expensive whether it be on the least of the failures or the most extensive.

DESIGN

Design also has considerable influence on construction failures. Indirectly, design can be the cause of many failures attributed to workmanship. Unless a design is made to suit the general methods and procedures to be used in construction, it can be the cause of many of the notches that are introduced in the course of construction. Care must be taken in any design to insure the best possible working conditions. A little extra effort in design will save untold trouble while the job is being done. There are certain items, such as leave loose for shipping machinery and the inevitable alterations, that the designer can do little about. These restraint joints have to be accepted and made

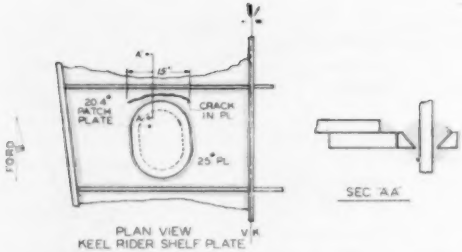

Fig. 28 Crack through restricted area between welds

satisfactorily. Other than that, the design should be such that erection and welding may start at a given point and work toward free ends. Care must be taken in the detail design to avoid all sharp corners and sudden changes in section that would act as a notch or geometric discontinuity, particularly where high stresses will be expected.

A few typical failures resulting from design details will best illustrate the dangers that can occur.

Figure 28: This crack occurred in the highly stressed and constricted area of the keel rider between the fillet weld of the patch plate and the fillet weld to the floor which formed a perfect notch for starting a crack. The patch plate was removed as well as the fillet weld to the floor adjacent to the crack. The crack was then chipped out and welded into a backing strip peening each pass of the weld. The fillet weld to the floor was placed and finally the patch plate replaced and welded with the weld between the end of the patch plate and the floor built up solid. This slight change of design eliminated all further trouble in similar locations on the same ship while everyone welded only with fillet welds cracked.

Figure 29: This is a similar crack to that in the previous figure which occurred in a deck through the reduced area between the welds of the bulkhead and the stiffening ring when subject to the high stresses from welding. This crack was repaired by chipping out and welding. The location of every piece on the ship, no matter how unimportant it may be in the final strength of the ship, should be given careful considera-

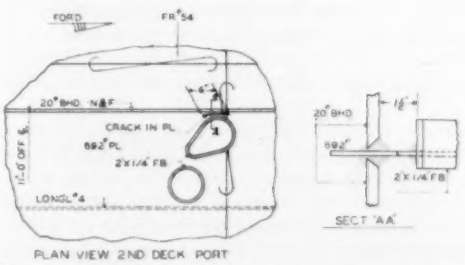

Fig. 29 Crack through deck in restricted area between welds

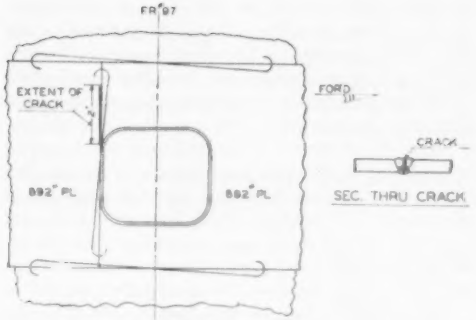

Fig. 30 Cracked deck butt

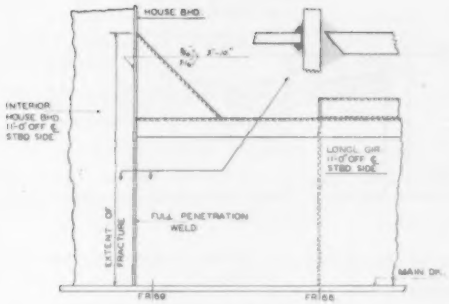

Fig. 32 Hatch girder connection to house side; stbd. side looking inboard

tion if we are to be sure that it will give no trouble during construction.

Figure 30: This butt fell in line with the edge of the hatch opening which was cut full size prior to welding the butt. In welding toward the sharp edge of the corner of the radius a sound weld was not deposited, which proved a starting point for the crack in the butt. The crack was merely chipped out and the butt rewelded. However, the crack could have been avoided if the butt had landed in the middle of the hatch opening or if the opening had not been cut or at least had only been cut undersize until after the butt had been welded.

Figure 31: Cracks occurred in butt welds made in forming the channel sections of the boat davits when these davits were welded in place on the ship. These welds were square butt welds without 100% penetration but were adequately strong for the davit. Unfortunately, these welds landed directly in way of the weld to the ship at the top of the shell and broke under the stress from this superimposed weld. These cracks were chipped out and the butts rewelded. Any concentration of welds are poor design but if such a concentration is necessary all welds should be sound and of equal strength.

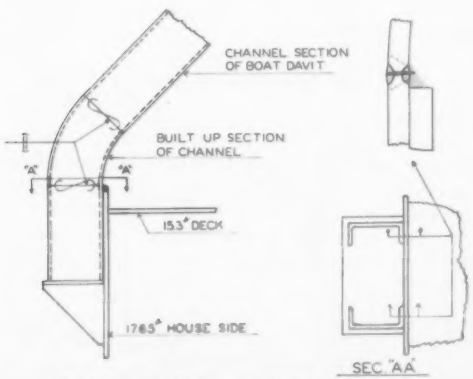

Fig. 31 Boat davit connection to house side

Figure 32: This is the design of a hatch girder in way of the house front on a ship presently under construction in this country. As you will see the hatch girder with full penetration welds is backed up inside the house by a light bulkhead with a continuous weld one side and an intermittent weld on the other. These fillet welds cracked following flame straightening of this interior bulkhead. Similar designs to this on previous ships have cracked in the same locations both in launching and in service. Good design must provide for a continuity of strength or a gradual tapering off of section rather than an abrupt discontinuity as shown here. Although these welds could be chipped out and repaired, a better repair would be to add a heavier insert in the interior bulkhead to allow for carrying the strength through the bulkhead.

Failures from design are every bit as much trouble and expense as those from bad workmanship. Every improvement and simplified detail in design that can help eliminate a failure during construction is worth while.

MATERIALS

The third and final cause of failures and repairs is the materials that we use.

It would be possible to give many examples of cracked welds that have resulted from welding materials that do not meet specification requirements, but I feel that is unnecessary. Very often, if the nature of these questionable materials were known, welding requirements could be altered to permit satisfactory welding. This is particularly true with the advent of alloy steels to be welded. I would like to emphasize again the importance of adequate material control so that the men working the material will know what to do with any piece of material. I think that we in the shipbuilding industry might do well to follow the practice of many other industries that keep a far more accurate check of their materials than we do.

Alloy steels, particularly those with which we work most in the shipbuilding industry, are subject to underbead cracking. In fact during World War II it was

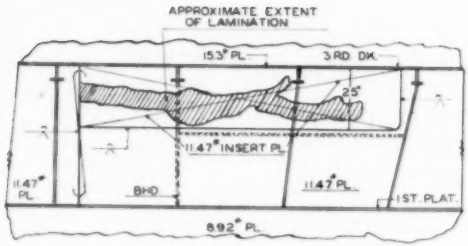

Fig. 33 Laminated plate bulkhead No. 151½; port side looking aft

one of the major cracking problems in welding special-treatment steel and high-tensile steel. However, with the use of the new mineral-covered low-hydrogen electrodes this type of cracking should be kept to a minimum. Consequently, I will not go into any detail on this problem, but rather refer any who may be interested in the development of these new electrodes and the past experience with chrome nickel type electrodes to the many recent articles that have been written on this subject.

Although defective castings cannot be termed a structural failure, they definitely are a structural defect and require repair. Unfortunately, porosity, shrinkage cavities and cracks are all too prevalent, particularly in the larger, more intricate castings. And, too often, these defects are not discovered, even with the closest of inspections, until rough machining, and many times final machining is under way. The cost of these welded repairs is forcing the trend toward weldments and the joining of sounder small castings by welding.

Laminated plates fall into a similar category to defective castings except that many times the cure is more drastic and requires the complete replacement of material. If the lamination is of the so-called surface variety, the surface of the plate can often be built up by welding or if of a minor depth, 1/32 in. or less, the surface can be faired in by chipping or grinding. Deeper laminations unless of very limited extent usually require that the material be replaced. And that can be expensive. The following typical repairs involving laminated plates will show that every effort to discover laminations by inspection prior to or during shop fabrications is worth while.

Figure 33: A section of the bulkhead was found to be laminated as shown and about half way through the plate. The laminated section was removed and a new section of bulkhead plate inserted. A careful inspection of the plate edge prior to preparation for welding the butt would have discovered the defect before the bulkhead was erected and partially welded.

Figure 34: The lamination in this longitudinal was not discovered until the after butt of the longitudinal was prepared for welding. This, despite the fact that the lamination extended the full length of the longitudinal and should have been seen at the lightening holes or forward butt. The longitudinal had to be replaced, which not only meant chipping out all the weld connecting the longitudinal to the shell, inner-bottom and floors, but also cutting a 10-in. section out of each floor to permit removal of the old material and erection of the new longitudinal which was fabricated in two sections. A little more careful observation during shop fabrication and assembly would have saved a costly repair.

Figure 35: To be fair and show the good as well as the bad and to end on a little brighter note, this is a lamination that ended most conveniently to permit a welded repair. The small laminated area was built-up weld metal and the exploratory holes which had been drilled to discover the extent of the lamination was plugged by welding.

Again, I can only say that the repair of a material failure or defect can be just as expensive as the repair of a failure from workmanship or design. The selection and use of the best available materials and control of these materials to insure a uniform high standard of quality will help keep failures to a minimum.

The failures that I have discussed are illustrative of what has and what can happen.

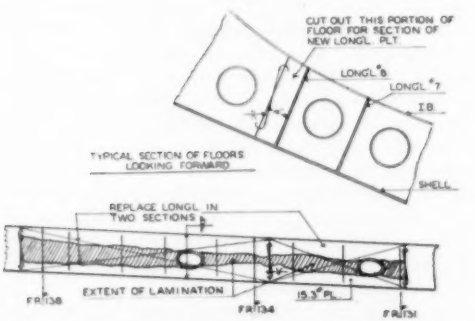

Fig. 34 Laminated longitudinal No. 8; port side looking outboard

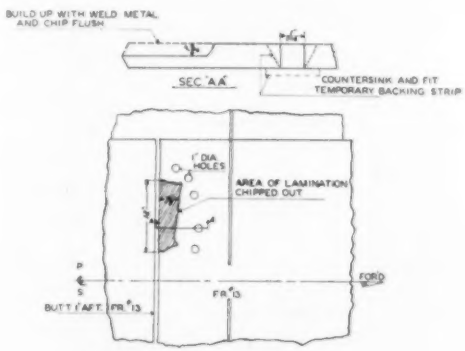

Fig. 35 Laminated flat keel plate

Every structural failure that occurs during the construction of a ship is important whether it is caused by workmanship, by design or by the materials used. Although, as I have previously stated, far more construction failures are caused and repairs required because of poor workmanship than either design or materials.

Fortunately, we can feel assured that the construction failures that do occur and the material defects that are found do not remain a hazard or danger to the ship in service provided they are properly repaired. Only those failures that remain undetected or unrepaired and those improperly prepared can ever become a start of a service failure.

But, disregarding this, economical shipbuilding demands that the structural failures and welded repairs in ship construction must be kept to a minimum.

Fusion Welding Techniques for Jet Aircraft Components

Techniques developed in fusion welding thin sheet and strip alloy metals in aircraft gas turbines

by Arnold S. Rose and Morton A. Braun

INTRODUCTION

THE large-scale production of components for aircraft jet engines imposes upon the welding engineer the responsibility for establishing welding procedures which are not only capable of use in mass production but which will yield metallurgically and structurally sound welds. Presented herein is a review of some of the techniques used and experiences gained in the fusion welding of the relatively thin sheet and strip alloy metals used in aircraft gas turbines. Mechanized welding processes under discussion include: (*a*) inert-gas-shielded arc, (*b*) shielded-inert-gas metal arc and (*c*) submerged arc as applied to stainless steels, high-temperature alloys, low-carbon steels and low-carbon, low-alloy steels.

INERT-GAS-SHIELDED ARC WELDING

The welding of stainless steel sheet with the inert-gas-shielded tungsten arc is a fundamentally well-established procedure with obvious advantages for aircraft welding. Freedom from slag and oxidation, minimum weld bead deposit and ease of handling are outstanding features. For high-production welding it may be readily adapted for nearly automatic operation. Such an application was developed for welding turbine

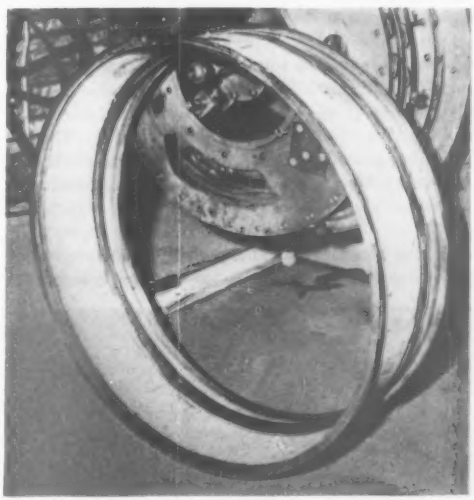

Fig. 1 Turbine casing assembly and welding fixture

casings for the GE, J-47 engine, using the stabilized grades of 18–8 stainless steel; AISI Types 347 and 321 in thicknesses of approximately 0.093 in. In this instance, it is required to butt weld four circumferential joints on the casing assembly which is 36 in. in diameter, as shown in Fig. 1. The assembly is tacked manually with an inert-arc torch on an outwardly expanding fixture mounted on a standard rotating welding positioner. Accurate positioning of each joint over shallow grooves in the stainless steel back-up bars is ensured by this type of fixturing. As shown in Fig. 2, the welding head is brought to the desired height and

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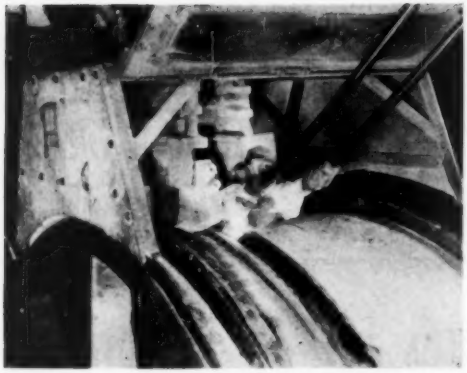

Fig. 2 Inert-arc torches located over circumferential joints

alignment over the joint. Arc start is provided by means of a high-frequency spark between the $^1/_{16}$-in. tungsten welding electrode and the work. Filler wire of $^1/_{32}$ in. diameter, Type 347 analysis is added to the molten pool at a rate of 35 ipm by means of wire-feed apparatus mounted overhead. Gas shielding is provided by argon flow of 30 cfh, and welding proceeds at a rate of 11 ipm with d-c straight polarity of 170 amp.

During the course of welding the joint, operator supervision is limited to minor adjustments of the arc length and alignment over the seam. Because of these minor demands upon his attention, it has been found feasible to weld two joints simultaneously. The sequence of operations involves starting the arc over one seam and, following a few inches of welding, initiation of the second arc. This setup, in operation, is shown in Fig. 3.

Having herewith provided a highly productive welding procedure for the stainless steel turbine casings, the

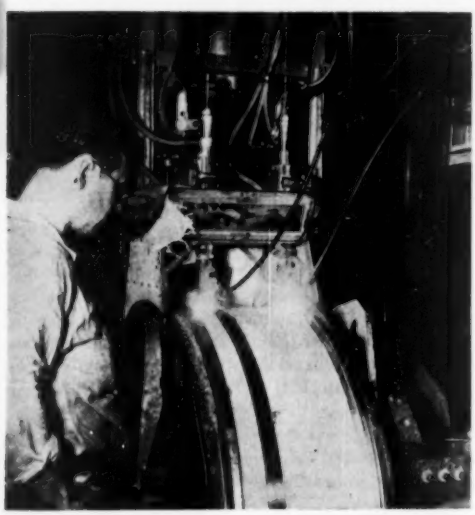

Fig. 3 Welding of turbine casing in progress

Fig. 4 Photomicrograph of austenitic structure of cracked welds AISI Type 347

Fig. 5 Photomicrograph of intercrystalline cracking of weld metal AISI Type 347

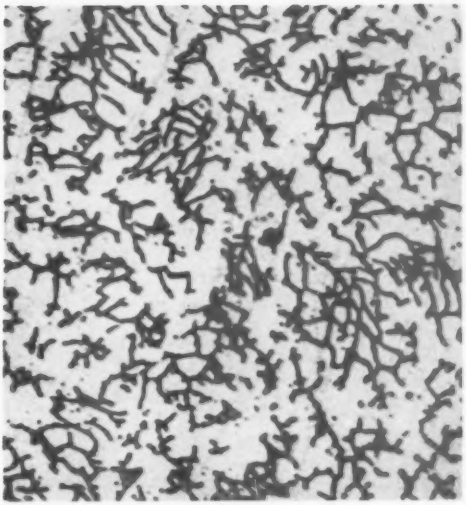

Fig. 6 Photomicrograph of crack-free weld metal in AISI Type 347 containing 8% ferrite

responsibility for weld quality becomes of prime concern to the welding engineer. Following the establishment of this welding setup, thousands of Type 347 casings were welded without serious difficulty. However, during one short period, an epidemic of transverse cracking in the weld bead was troublesome particularly in view of the previously noted freedom from cracks. Microscopic examination of the welded zone of those turbine casings which had cracks in the weld areas revealed that the structure was wholly austenitic in nature as shown in Fig. 4. The cracking which occurred was intercrystalline as may be seen in Fig. 5.

Confirmation of the entirely austenitic structure of the weld was given by measurements made with the Aminco Magne-Gage. This instrument is capable of determining, following suitable calibration, variable amounts of ferrite based upon the residual magnetism of this phase. Samples which showed weld cracks were examined with the Magne-Gage and readings taken on areas adjacent the cracks indicated complete absence of ferrite.

At this point, reference was made to casings welded prior to the period when cracks became prevalent. Examination of crack-free welds disclosed a structure (Fig. 6) which contained approximately 8% of ferrite in the austenitic matrix. These samples were then Magne-Gage checked and the microscopic estimation of ferrite content was confirmed.

This contrast between the ferrite values obtained in cracked and crack-free welds was in substantiation of information developed by several previous investigators regarding the propensity of wholly austenitic welds to cracking. The problem of eliminating weld cracks in this Type 347 parent metal resolved itself then into ensuring that sufficient ferrite existed in the weld to prevent cracking. Furthermore, since the ferrite content is a function of the amount of chromium and columbium stabilizer present in ratio with the carbon, nitrogen and nickel, it is important that these elements be controlled by specification in both parent metal and filler wire.

A series of tests was run to establish some data on filler wires of varying compositions. A sample of the $^1/_{32}$-in. Type 347 wire which had been used for production welding was melted down under an inert atmosphere into a bead and examined. Upon microscopic examination, only a negligible amount of ferrite was noted and this was confirmed with a 0% Magne-Gage reading.

Various sample butt welds were then made with this wire using parent metal stock of varying ferrite values. The accompanying Table 1 indicates the effect of the filler wire upon the composition of the resultant weld. In order to investigate this effect more thoroughly, a survey was made of other grades of filler wire. These included: (*a*) Type 347 wire—analysis controlled to ensure sufficient columbium; (*b*) 19 chromium–9 nickel-tungsten-molybdenum—with additions of the strong ferritizers tungsten and molybdenum; (*c*) 29 chromium–9 nickel.

Table 1

Sample No.	*Magne-Gage values of butt-welded parts, % ferrite*	*Magne-Gage values of resultant weld metal, % ferrite*
1	0 welded to 0	0
2	0 welded to 6	4
3	7 welded to 10	8

Values for butt welds made with these wires on typical 0% ferrite parent metal are given in Table 2.

Table 2

Sample No.	*Wire type*	*Magne-Gage value of resultant weld metal, % ferrite*
1	Type 347—low columbium	0
2	Type 347—high columbium	4
3	19–9—tungsten-molybdenum	18
4	29–9	18

Evaluation of these results indicates that a high columbium Type 347 wire will produce weld beads with sufficient ferrite to minimize intercrystalline cracking even on 0% ferrite parent metal. A turbine casing assembly was welded with the 19–9 tungsten-molybdenum filler wire to determine the effect of its strongly ferritizing constituents. Although a high ferrite value weld resulted, the wire proved unsatisfactory from a usability standpoint, because of erratic penetration and poorly formed underbead.

With the disappearance of Type 347 material and its replacement by Type 321, a new set of circumstances

arose. Examination of dozens of heats of this alloy, which is stabilized with ferrite-promoting titanium, has shown that in every instance sufficient titanium is present to ensure the presence of ferrite in the weld metal. Further, very practical evidence of the role of this phase in the elimination of weld cracks is given by the complete absence of this type of cracking after the introduction of this alloy.

SHIELDED-INERT-GAS-METAL ARC WELDING

While the inert-gas-shielded-arc welding process, as just discussed, is an ideal method of fabricating sheet material in stainless steel, one of its serious limitations lies in the fact that it is best suited to the welding of material 0.125 in. thick or less. Though the majority of the components of an aircraft gas turbine are fabricated from material below this thickness limit, very often it is found necessary to weld heavier gage material.

Previous to the development of the shielded-inert-gas-metal arc process, a choice of welding process for these heavy gages was among the multiple pass inert arc, the flux-coated metallic arc process or the submerged arc-welding process. Each of these methods, while producing sound welds, inherently involved elements which are deterrents to high-production work. Thus, the advent of the shielded-inert-gas-metal arc process filled an important gap in the welding field. It combines the cleanliness and desirability of inert-gas shielding with the high current density and high-speed continuous welding of the submerged melt process and the flexibility, quality and penetration available with manual flux-coated electrodes. Briefly described, welding with this process is accomplished utilizing a substantially inert-gas-shielded electric arc which is maintained between the work and a continuously fed consumable electrode wire.

The fabrication of an actuating air cylinder to be used in a jet engine afterburner afforded an opportunity to take effective advantage of the features of this process.

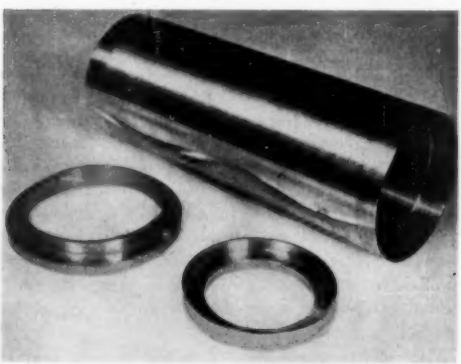

Fig. 7 Completed air cylinder

The finished part (Fig. 7), a stainless steel cylinder 18 in. long, 6 in. in diameter with a $^1/_4$-in. wall thickness, was manufactured from $^5/_{16}$-in. thick plate material (AISI 321), which was sheared to size, rolled into cylindrical shape and welded longitudinally. Inasmuch as the cylinder bore has to be ground and lapped, an X-ray quality weld, free of all porosity defects, was required. The joint edges were prepared for welding by beveling to form an included angle of 90° with a very small section of land at the root (less than $^1/_{32}$ in.) to aid in preventing overlapping (Fig. 8). The part was rigidly fixtured over a copper chill bar which contained a relief groove approximately $^1/_4$ in. wide and 0.040 in. deep in the shape of a smooth curve.

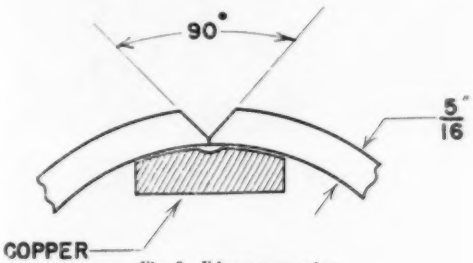

Fig. 8 Edge preparation

After tacking the finish end of the joint to prevent separation of the edges, the welding proceeds at a rate of 9 ipm. The electrode wire in this case is $^1/_{16}$-in. diameter Type 347 stabilized grade of 18-8 stainless, straightened and layer level wound on a 9-in. diameter spool. Direct-current, reverse polarity of a magnitude of 300 amp is used for welding and the shielding gas which is sigma grade (5% oxygen bearing) argon flows at a rate of approximately 35 cfh. Wire feed is automatically initiated when the arc is struck and its rate is adjusted remotely to match the melting-off rate produced by the welding current. Using a weaving motion, complete penetration is obtained at the same time as the prepared groove is filled, completing the weld in a single continuous pass. Figure 9 illustrates this setup.

It has been estimated that this joint, which by the shielded-inert-gas-metal arc process, is welded in 2 min, would require no less than 45 min to weld using the flux-coated metallic arc process with the resulting freedom from defects and inclusions being doubtful. An additional point of importance should be made with regard to the shielding gas, sigma grade argon. It has been shown that the addition of small percentages of oxygen to the argon shielding gas materially alters the characteristics of the arc, notably affecting the molten droplet rate of the electrode wire which is increased by about 30 times over the rate with pure argon. In addition, higher welding speeds may be used without encountering those defects usually associated with excessively fast welding, such as undercutting and

Fig. 9 Inert-gas metal-arc welding of cylinder shown in Fig. 7

lack of coalescence in the weld metal. There was, however, some fear that it would be impossible to transfer the stabilizing elements across the arc due to their great affinity for oxygen. In order to evaluate this transfer sample welds were made with Types 321 and 347 consumable electrode wires under sigma grade argon shielding gas, and the samples were checked both microscopically and with the Magne-Gage as described previously. It was found that the stabilizing elements could, in fact, be deposited with no significant loss due to the presence of oxygen in the shielding medium as indicated by the amount of ferrite found in the weld.

As is often the case, when new processes are introduced, some difficulties became obvious in the production shop which had not been previously recognized in the development laboratory. The first of these involved the stiffness of the stainless steel filler wire when used as a consumable electrode with the manual shielded-inert-gas-metal arc torch. While no arc length control is incorporated with this equipment in its present form, there is to a certain extent an inherent self-correcting factor in the process for changes in the length of the arc. Since this is the case, when an acute problem of rapid erratic changes in arc length developed, the condition of the $^1/_{16}$-in. diameter stainless steel wire was quickly isolated as the trouble source. It was found that when using annealed wire, the arc remained steady. On the other hand, the springy characteristic of unannealed wire caused it to repeatedly build up in the slack of the cable and then shoot out suddenly, thus causing the condition noted.

Kinking of the wire is also a serious problem, inasmuch as the diameter of the parts through which the wire must pass is held fairly close to the nominal wire diameter. Further, the fact that the wire required annealing served to increase the possibility of the wire becoming kinked. Therefore, it was necessary to reprocess the wire for use by layer level winding before satisfactory operation of the equipment could be attained.

The surface condition of the wire also should be mentioned. In the case of stainless steel, oxide scale in itself is not detrimental to quality welding. Therefore, a tightly clinging oxide scale, it was found, could be permitted to remain on the wire. Loose scale necessitated removal because of its tendency to flake off and clog the wire-feeding equipment eventually overloading the driving motor and interrupting the welding. Oil or grease on the surface of the wire are both common and seriously deleterious to the weld. Even slight traces of these substances have been shown to be the cause of weld metal cracks. In general, therefore, the wire must be annealed, straight, and free from oil or grease.

SUBMERGED ARC WELDING

Another important phase of the defense program has been the development and utilization of low-alloy substitutes for the critical types of material generally used in aircraft gas turbines. Fabricators of these engines and engine components were therefore faced with the problem of changing from the welding processes suited to the stainless steels and high nickel alloys to those procedures best suited to the lower alloy material. One such change-over was necessitated when certain models of the turbine casing assembly previously discussed were redesigned for manufacture using NAX high-tensile steel.

An investigation was conducted to determine the most satisfactory welding process for NAX material, specifically where used to form a circumferential butt joint in material 0.093 in. thick and 36 in. in diameter. This investigation demonstrated that satisfactory welded joints could be produced in NAX material by the following fusion processes: (*a*) inert arc, utilizing a fully killed filler material; (*b*) metallic arc, using low hydrogen coated electrodes; (*c*) submerged arc.

Of these, the most suited to high-production automatic welding is the submerged arc process. Four circumferential seams are welded in succession at a welding speed of 70 ipm as compared with a speed of 11 ipm for the inert-arc-welded stainless steel unit. An automatic welding head feeds the $^3/_{32}$-in. diameter low-alloy electrode at a rate controlled by an electronic voltage control unit which maintains at a constant level, a previously adjusted welding arc voltage. In this case 24 v are used at 300 amp direct current, reverse polarity. The current supply is a 400-amp selenium cell rectifier which delivers welding current at a constant level making it possible to maintain a virtually constant welding current and voltage. Figure 10 illustrates the welding setup. As shown in Fig. 11,

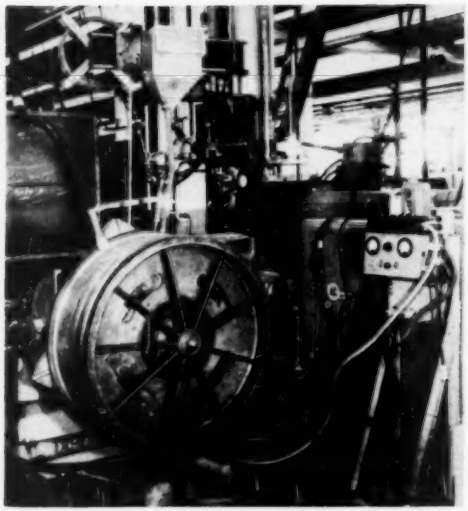

Fig. 10 Submerged arc welding setup

it might also be noted that the granular flux is gravity fed to the work just ahead of the welding nozzle and removed from the work, immediately after the weld is made, by a vacuum recovery unit which separates the dirt from the reusable flux which is then returned to the hopper.

One of the very important problems which faced the welding engineer during the development stage of this setup was the determination of the chill bar design. Stainless steel chill bars, which were a carry-over from the similar stainless steel unit, proved unsatisfactory from two standpoints. First, since the submerged arc process is inherently a "deep-penetration process," it was found that more often than not, the stainless steel chill bars were welded to the casing assembly, in which case they had to be removed by machining, and replaced; obviously, a completely unsatisfactory condition. Moreover, the back-up groove contour which for inert arc welding was a $^1/_4 \times 0.03$-in. rectangular cut, was not suited to this type of welding. Copper chill bars were found to be a satisfactory solution to the material problem. Groove size was determined by trial and error to be $^1/_4$ in. wide and a 0.040-in. deep arc. Shallower grooves produced a pitted condition adjacent to the underbead due to the gas entrapment and deeper grooves allowed molten metal to run away from the puddle at various intervals leaving gaping holes in the weld bead. One problem still remained regarding back-up bars, namely the solidification in the groove of a thin flux layer which was extremely tenacious. Grinding this layer away manually was unsatisfactory, inasmuch as the size and shape of the groove were altered to such an extent that the bars became unusable for subsequent welding. Solution of this rather difficult problem involved the use of a "Virgo" caustic molten salt descaling bath which effectively removes the slag and excess flux as well as copper oxide from the chill bar without attacking the bar itself. As a result, there are no dimensional changes and the bar may be used repeatedly.

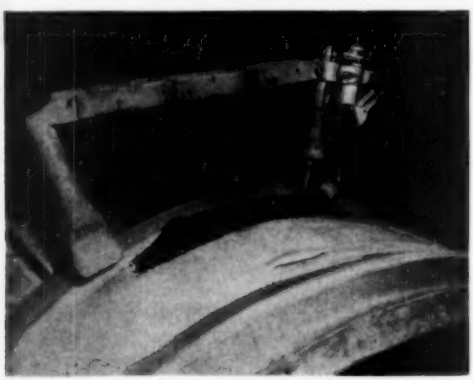

Fig. 11 Close-up of submerged arc-welding equipment showing flux nozzle, electrode tip and vacuum pickup nozzle

Another interesting problem regarding the manufacture of this assembly developed because of the high welding speed involved. A time study revealed that the welding time per assembly was only a small percentage of the total time, assembling and tacking of the parts accounting for the major portion. This, of course, was not conducive to making most effective use of the welding equipment. Two possible remedies to this situation presented themselves, namely, to make the welding equipment movable so that it could be used on more than one fixture or, conversely, to make the fixture movable to accomplish the same result. The second alternative was decided upon because of the critical nature of the positioning and mounting of the welding head, and a design was formulated which provided for the mounting of two complete rotating positioners with welding fixtures attached, on a single set of tracks. Thus, each positioner in its turn could be moved into position under the welding head, and it would then be possible to double the output of units without adding another welding head and flux recovery unit.

INERT ARC WELDING OF LOW-CARBON STEEL

An outstanding example of the scope of activity of the welding engineer is illustrated by the problems presented in the welding and forming of a guided missile nose section fabricated of low-carbon SAE1010 steel. The welded unit is fabricated as a straight truncated conical section by joining two longitudinal seams using 0.031-in. thick sheet material. Following welding, the cone is hydroformed into an ogive shaped nose shell (Fig. 12): the forming requiring a maximum change in peripheral dimension of 15%.

Fig. 12 Completed nose assembly

The welding of the steel units was set up using an inert-arc tungsten electrode welding torch fixed on an automatic side beam travel carriage with filler wire addition from an attached wire-feed apparatus. The conditions of welding are as given in Table 3. The

Table 3

Material	SAE1010 steel	Current, amp	130
Material thickness, in.	0.031	Welding speed, ipm	20
Filler wire	CMS 32	Filler wire rate, ipm	10
Filler wire diam, in.	1/16	Argon flow, cfh	30

welds which result from such a setup were found to be excellent from a strength, ductility and porosity standpoint. These test data were borne out to a great extent by experience with the forming of the cone, in that only a low percentage of failure occurred. In those instances where the cone failed during the forming operation, it was found that the fracture originated in a section of the parent metal nearby the actual weld bead. A typical failure is shown in Fig. 13. Inasmuch as only occasional failures occurred, analysis of the problem again was made on the basis of comparison between successfully formed and fractured cones.

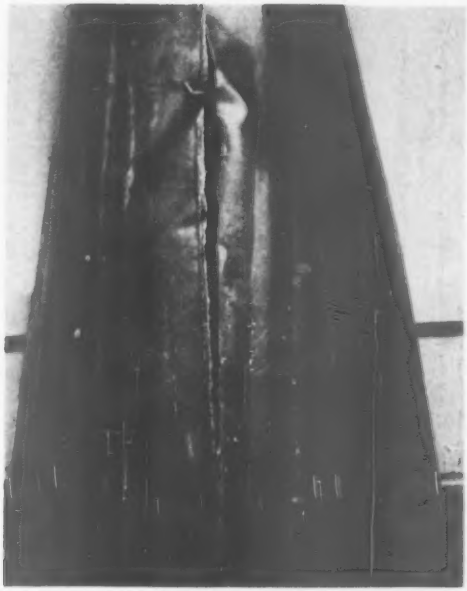

Fig. 13 Forming failure adjacent to weld in nose assembly

Fig. 14 Photomicrograph showing critical grain growth in low-carbon steel

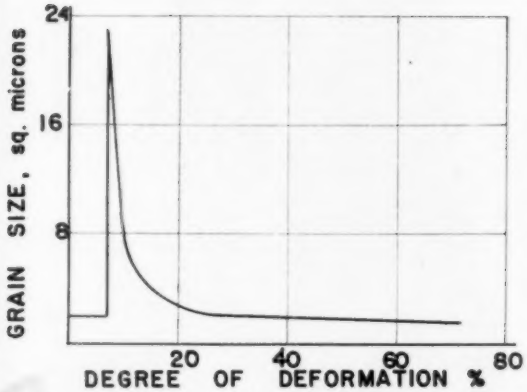

Fig. 15 Critical grain size curve for low-carbon steel

Metallographic examination of a cross section of a good cone shows the dendritic structure of the weld metal and the normal moderately coarsened grains of the parent metal adjacent the weld. By contrast, it was noted that those cones which did not withstand forming pressure had a severely coarsened parent metal grain size adjacent the weld. The extent of this coarsening is shown in Fig. 14 where it may be noted that only a single grain occupies the entire cross section of the parent metal. The cause of the forming failure is understandable in view of this excessively coarsened grain size and its accompanying loss of ductility.

The cause of this occasional grain growth is found to be a function of the reheating of lightly cold-worked steel. It is a well-known metallurgical phenomenon that there exists a critical amount of cold working which will induce extremely large grains when the steel undergoes such heating as occurs in the areas adjoining a weld bead. Figure 15 shows this condition graphically for low-carbon steel. The critical deformation which causes the formation of large grains corresponds to approximately 7% cold working. Amounts of cold work below and above this critical amount are found to have much smaller effect upon the recrystallized grain size following heating. It was, therefore, reasoned that the excessive grain size adjacent the weld in the failed cones was caused by small amounts of working such as might occur as the pieces were rolled, hammered and bent into shape.

Fig. 16 Photomicrograph showing normalized grain structure of sample similar to that shown in Fig. 14

With recognition of the source of the failure, elimination of the coarse grain structure solved the problem. This was accomplished by normallizing the as-welded structure at 1750° F and air cooling, followed by a 1200° F tempering. The refined grain structure following this heat treatment is shown in the photomicrograph, Fig. 16. Since the institution of this heat treatment, forming of the normalized and tempered cones has been successful.

How to Save Cost by Designing for Structural Welding

Factors affecting cost of a number of welded connections frequently used for welded structures

by Alfred E. Pearson

THE future owner of a new building is vitally interested in only three aspects concerning its construction and these are the building's suitability for a particular purpose, the time required for its construction and its cost. From the owners point of view, welding favorably affects all three aspects mentioned and many others as well, but a building must be designed for welding before its advantages can be realized.

Any competent engineer is well acquainted with the engineering principles required to determine the size of structural members no matter how they are joined together, but there are still some competent designers who are not familiar with welded connections and may hesitate to specify welding on that account. However, there is no need for hesitancy because authoritative details of a variety of welded connections are available in publications readily at hand in practically all engineering offices. Some of the sketches illustrating this paper have been adapted from details shown in the AWS "Welding Handbook," the "Airco Manual" and other publications. On the other hand, specific cost information is not so readily available and this makes it difficult for a designer with no previous experience with welded work to evaluate his design.

The present paper presents a discussion of factors affecting the cost of a number of welded connections frequently used for welded structures. In some cases the difference in cost, of what might be termed competitive connections, is given in dollars and cents while in other cases only the relative cost is indicated.

CHOICE OF FRAMING

Building codes permit a choice of three types of framing. These are simple or conventional framing, rigid or continuous framing and semirigid or partially restrained framing. All three types represented in Fig. 1 have their own particular characteristics that affect their cost.

Simple Framing

In simple framing, the ends of beams and girders are connected for shear only and are assumed free to rotate under load, but suitable provisions must be made to resist lateral movement caused by wind or other forces. It is assumed in the design that all stresses are kept well within the elastic range of the material but actually some nonelastic, but self-limiting, deformation will occur, usually in the top connection, but under forces that do not overstress the welds. Simple framing is comparatively easy to design, safe load tables and standardized connections having been devised for both riveted[1] and welded[2] work. Also fabrication labor is comparatively much less.

A disadvantage of simple framing, when compared with other types of framing, is that beam sections are not only heavier but usually they are deeper, resulting in added height that will average about four inches for each story of a tall building. This increases the cost of exterior walls to a certain extent and of course applies to all simply framed buildings whether welded or riveted.

Continuous Framing

The design assumption for continuous framing is just the opposite of that for simple framing in that no relative beam end rotation is assumed. Fully fixed ends cause the maximum moment induced in continuous beams to be only two-thirds of that induced in simple beams under the same loading conditions. An appreciable saving of main member material can result from either full or partial end restraint. In simple framing the maximum moment occurs at the center of a beam with no moment at the ends, thus the end

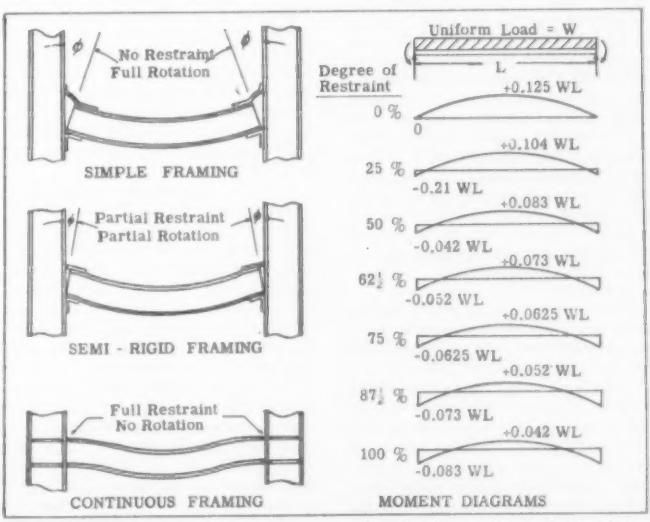

Fig. 1 The three types of framing permitted by building codes and the relative distribution of moment in each type

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connections must transmit only the shear load to the columns, but the maximum moment in continuous beams occurs at the beam end so the connection for continuous beams must transmit both shear and moment forces. Therefore, it follows that a restrained connection will be more expensive than a simple connection and that this additional expense will offset some of the saving in the main member material cost.

Semirigid Framing

Semirigid framing, as the name implies, provides a degree of restraint at the beam ends that falls somewhere between the completely rigid framing and completely flexible framing already discussed. The use of semirigid framing is limited by the present AWS Building Code because it is permitted only upon presentation of evidence that the connections are capable of resisting definite moments without overstress of the welds, and the main members must be proportioned according to the minimum known end restraint affected by the connection. Just what constitutes satisfactory evidence may be debated by Building Commissioners who must approve designs. Nevertheless, semirigid framing offers the greatest possible economy in steel construction.

If a connection could be devised to accurately provide 75% rigidity then the maximum moment induced in a beam would be only 50% of that induced in a simple beam and nearly 25% less than that induced in a continuous beam under the same loading conditions. Unfortunately, the rigidity of various connections cannot be pinpointed that accurately at present so to be on the safe side it has been suggested[3] that the beam be proportioned on the basis of 50% rigidity for the connection but that the connection be designed for 75% rigidity. Such a design would result in no main member material saving over a fully rigid design because the maximum moment would be unchanged, merely moving from the end to the center of the beam. However, it would result in a saving of approximately 25% in connection material, if connection material were used to develop the moment in the fully rigid connection.

COMPARISON OF SIMPLE CONNECTIONS

To evaluate various types of framing and applicable connections used in building construction a study was made of the cost, based on steel at the mill and average wage rates prevailing in the Birmingham district, for six different connections for simple framing and two for continuous framing that will be discussed later. All eight members were designed for the same span and loading conditions. The study included all material, shop and field labor costs for furnishing and fabricating a typical beam with its two end connections

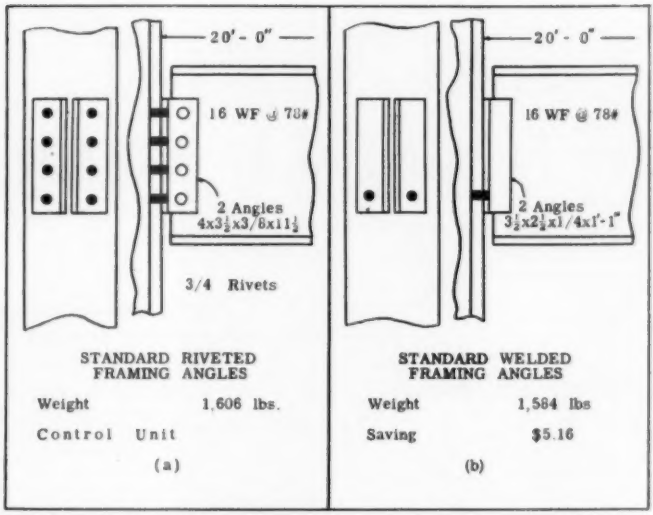

Fig. 2 Comparison of similar beams with standard riveted and standard welded framing angle connections

actually used in a five-story building. No overhead, profit or freight charges were included in the cost totals.

A designer is mainly interested in the saving one design will provide over another Therefore, only the difference in cost or the saving resulting from the use of welding instead of riveting is given.

The control unit for the comparison is a simply framed, 16-in. WF at 78 lb beam required by the original design that indicated an AISC Standard B4 riveted framing angle connection. The weight of the beam, connection angles and rivets totaled 1606 lb. There is no standard riveted seat angle for reactions over 35 kips and as the design assumed a reaction of 42.5 kips the riveted seat was not considered. A riveted stiffened seat is obviously heavier and more expensive, therefore it was not considered either and the most economical riveted connection was used. It is shown in Fig. 2 (*a*).

After the comparison was completed it was found that the standardized welded framing angle shown in Fig. 2 (*b*) was the most economical of all the flexible connections investigated. The direct saving amounted to $5.16 for the beam and its connections. All of this saving resulted from the welded connection as the beam size was not changed. The weight of the beam and connections was 1584 lb.

The welded framing angle connection provides the lightest flexible connection, seldom conflicts with clearance requirements and is well suited for beam-to-girder connections, but it does have less desirable features that must be considered by the designer. These features are (1) beams under 18 in.-deep cannot be fully developed over short spans with the standard connection, (2) columns must be punched or drilled for erection holes but a scheme for overcoming this will be described later, (3) comparatively more field welding is required and (4) strict adherence to the relation of minimum fillet weld size to maximum thickness of material joined will preclude their economical use for many beam-to-column flange connections. Quite often the indicated framing angle is only $^1/_4$ in. thick but the column flange exceeds $^3/_4$ in., necessitating a $^5/_{16}$ in. weld size. Thus, the framing angle would have to be increased in thickness just to permit the application of the required weld size.

The second less expensive flexible connection proved to be a T-seat fabricated from two plates as shown in Fig. 3 (*a*). The total weight of the member was 1592 lb and the saving amounted to $4.54. The T-seat can carry comparatively larger reaction loads than other types of connections, but it weighs more than the comparable framing angles, particularly when the required flexible top angle is considered. However, less field welding is required and that is in the flat position while framing angles require vertical field welding.

The T-seat cut from a wide flange section, shown in Fig. 3 (*b*), was the fourth less expensive flexible connection investigated. The complete member weighed 1606 lb which is exactly the same as that of the riveted beam, but still the welded beam cost $3.66 less than the riveted one. The built-up T is less expensive than the T cut from a wide flange section because plate material is cheaper than wide flange material and the flange of the built-up T can be reduced in thickness thereby saving in

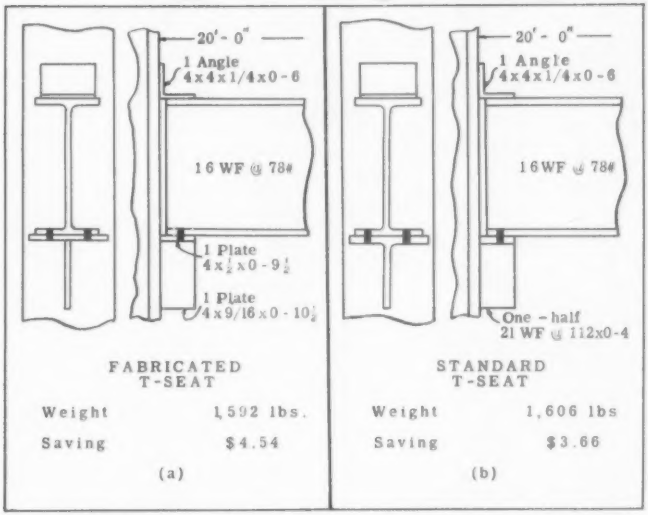

Fig. 3 Welded T-seats for similar beams, showing saving over cheapest riveted beam

weight and because the plate material can usually be sheared rather than flame cut.

A modified framing angle shown in Fig. 4 (*a*) eliminates erection holes in columns. Where beams have other holes in the web the additional holes for the end connections can be made at very little added cost. This type of connection often permits the beams to be punched and then shipped with no further fabrication, while the columns can by-pass the punching or drilling operation. All shop fitting and welding that ordinarily would be distributed between the two pieces can be performed on the one member. The weight of the modified framing angle is somewhat greater than that of the standardized framing angle and field welding is increased about one-third but it still results in a saving of $3.81 over the riveted beam. The member weighed 1588 lb. The elimination of some handling of the beam in the shop results in a definite though intangible cost reduction not reflected in the cost comparison.

The least favorable welded connection investigated proved to be the standardized angle seat shown in Fig 4 (*b*). Nevertheless, the beam and the seat connections still provide a saving of $3.59, even if they do weigh 1624 lb or 18 lb more than the riveted member. This weight difference is in the connection material which cost 55 cents more than the riveted material but the saving in shop and field labor caused by welding more than offset the extra material cost. Previous studies made by the author indicate welded seat angles should perhaps be limited to a maximum reaction load the same as riveted seat angles. In the case of welded seats this is merely for economic reasons, to prevent the use of more material for welded connections than for the most economical riveted connection. Welded seats subject to reaction loads lighter than that now being considered compare much more favorably. The limitations on the use of riveted seat angles for large reaction loads are based mainly upon technical reasons.

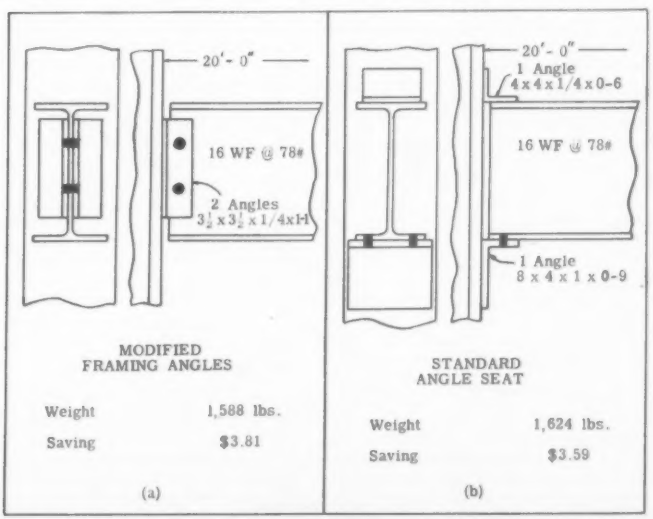

Fig. 4 Other welded connections to be compared with similar riveted beam shown in Fig. 2(a)

COMPARISON OF CONTINUOUS CONNECTIONS

For the investigation of continuous framing the main member was redesigned to take advantage of the reduced maximum moment resulting from restrained end conditions. This permits the use of a lighter or shallower beam or a combination of both. A 16-in. WF at 58 lb continuous beam met all requirements to sustain the same load over the same span as the 16-in. WF at 78 lb used as a simple beam. This resulted in a weight reduction of 400 lb in this particular case. If a shallower beam had been selected to reduce the height of the structure a smaller reduction in weight would have resulted.

Stiffener plates between the column flanges and in line with the beam flanges were deemed necessary to insure maximum restraint at the connection. The stiffeners on only one column were included in the cost analysis, however, because the same stiffeners also serve beams framing into the opposite face of the columns.

Figure 5 (*a*) shows a type of continuous connection investigated that employed direct welding of the beam flanges to the column to restrain the beam ends. The modified framing angle was used for the shear connection and backing strips were provided to assure complete fusion into the root of the butt weld at the flanges. Bars could have been used instead of angles for the shear connection and would have resulted in a small additional saving. In comparison with the previously discussed riveted member this rigid beam provides a saving of $14.04. In comparison with the most economical welded beam, simply framed, there is a saving of $8.88. This continuous connection requires more connection material and the labor is much more than for any of the simple connections. All of the saving results from the reduction in weight of the main member. It should be brought out, however, that the particular beam chosen for the comparison happened to be in a very favorable position in the framing system in so far as

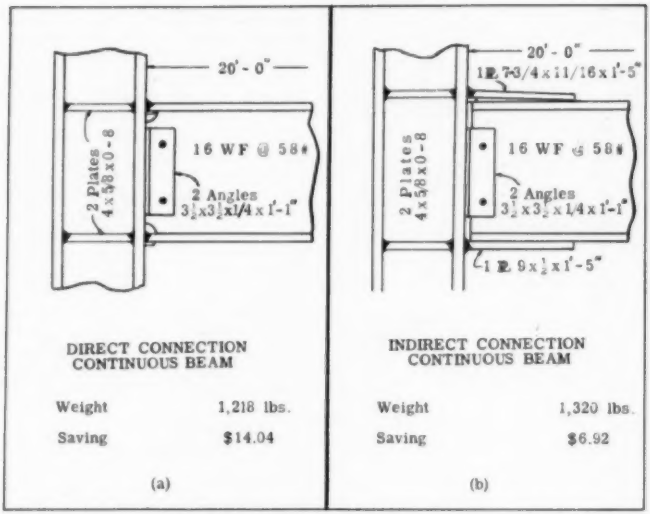

Fig. 5 Two types of continuous beam connections showing weight and saving over riveted beam of same load capacity

moment is concerned. The redesign for continuity did not reduce the size of all beams proportionately.

Another type of continuous connection is shown in Fig. 5 (*b*). Its connection material alone costs $5.00 more than that for the other continuous connection discussed and the labor will cost considerably more also, yet the beam in place costs $6.92 less than the riveted simple beam. The moment plates are used to prevent cutting the beams to exact length in order to butt weld the beam flanges to the column, but the comparison shows this is false economy. As the depth of columns may vary from the theoretical, a root opening greater or smaller than that desired for a direct connection may result, but the minimum desired root opening can be assured by cutting the beams shorter by the amount of expected overrun in depth of the column. This practice will cause a larger root opening when the column is of the correct depth or less, but not seriously so. The cost analysis for the direct connection of Fig. 5 (*a*) was based on a root opening of 1/4 in. for the beam flange weld.

No comparison was made for semirigid framing but if the beam were designed for 50% rigidity and the connections for 75% rigidity as has been suggested there would be some saving over the fully rigid member connected with moment plates, but none over the member with flanges butt welded directly to the column. As there would be no saving in main member material but approximately 25% saving in connection material and presumably labor, the suggested semirigid scheme would save about $6.00 over the fully rigid moment-plate connection but would cost nearly $1.00 more than the butt-welded connection.

BUTT WELDS VS. FILLET WELDS

Designers frequently ask fabricators what is the best type of joint to use from a cost standpoint. The superiority of butt joints with respect to developing the full strength of the base material, particularly when dynamic loading conditions prevail, is generally known, but the fact perhaps has been somewhat overemphasized and it probably should be emphasized in turn that fillet welds very definitely can be used to advantage. The full strength of a member does not always have to be developed at a connection; therefore, it is wasteful to do so. Intermittent butt welds are prohibited by specifications but intermittent fillet welds are permitted, thus fillet welds are more adaptable to providing only the strength necessary without undesirable overwelding.

A detailed cost analysis that considers all factors, including the higher cost of preparation and fitting of butt joints as well as the added material required by overlapping surfaces and the lower allowable stresses for fillet-welded joints, will show fillet-welded joints are more economical for material up to about 1/2 in. thick. This point will vary somewhat with the load requirements of the joint, but the analysis definitely indicates that many connections encountered in welded structures can be made more economically by fillet welding than by butt welding, even though more material is required. The graph in Fig. 6 shows the relative cost of 100% penetration butt joints and fillet-welded lap joints with 2 in. of overlapping material that develop at least 60% efficiency. An intermittently welded lap joint would provide even greater economy as would a "T" joint.

WIND CONNECTIONS

As mentioned previously, some provision must be made for simple framing to resist lateral movement caused by wind or other horizontal forces. Often this is accomplished in tier buildings by knee and diagonal bracing that can get quite complicated and expensive. Figure 7 shows a unique method of stiffening a column where a knee brace induces unusual bending moment. Such bracing consists of angles or channels shop assembled back to back and separated by small spacer bars. Plates capable of carrying the calculated

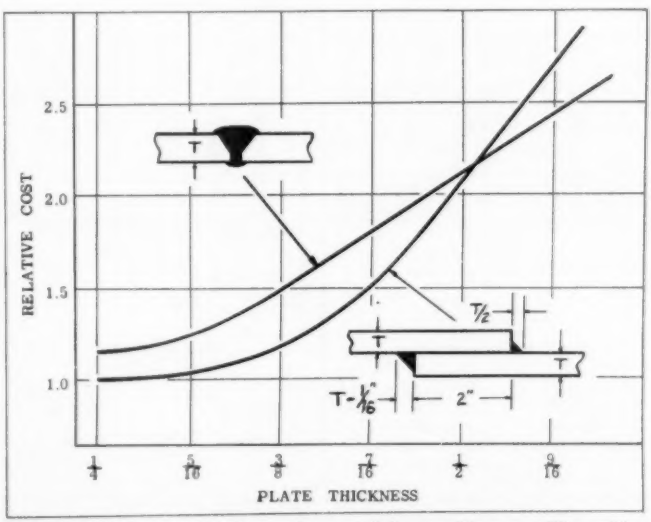

Fig. 6 Relative cost of labor and material, butt welds versus fillet welds

Fig. 7 Knee braces such as those shown here can be eliminated by use of alternate method of distributing wind moments permitted by building codes

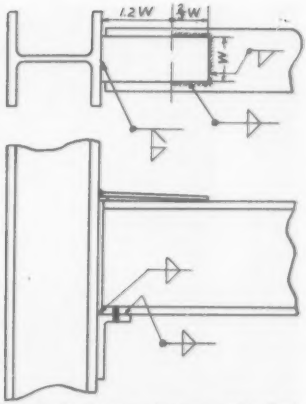

Fig. 8 Light top plate provides flexibility for simple beam action yet restraint against wind forces

Fig. 9 Butt-welded splice is economical for columns of the same nominal size

stresses are shop welded to the beams or columns and the bracing is erected and welded to them in the field; thus, two welds capable of developing the full tensile strength of the bracing must be made at each end of the member. For diagonal bracing, one of the two members in a panel is broken at the point of intersection with the other member to facilitate erection necessitating two more full-strength welds. As the allowable stress in members subject to wind loading can be increased one-third to 26,600 psi, the elements of the bracing are comparatively light but even though the allowable stress in the welds can be increased a like amount, the labor costs for fabrication and erection is proportionately higher. In some instances the field welding cost for diagonal bracing can be 50% of the cost of welding all beam end connections and column splices combined. This, of course, is reflected in a higher pound price for bracing material.

The AISC Specification and most building codes provide an alternate method of distributing wind moments in simply framed tier buildings that results in a substantial saving of labor and material required to transmit the wind forces. It should be noted the alternate method is permitted only if the connection is welded. Such a connection is shown in Fig. 8.

To apply this alternate method a top plate for beam-to-column connections is designed to carry only the wind stresses at the joint, using the 26,600 psi working stress permitted for members subject to wind forces. The larger moments induced by the gravity load on the beam to which the connection is attached will cause the plate to elongate by plastic deformation, thus allowing beam end rotation to satisfy the design requirements for simple framing. The initial gravity stresses in the plate are relieved by this deformation and the plate remains to safely perform its intended function, that of transmitting wind stresses only. The welds on the top-plate must, of course, be sufficient to permit deformation without overstress of the welds.

When the wind blows from one direction the top plate will be in tension and the bottom flange of the beams will exert a thrust against the column. Therefore, sufficient welding must join the beam and its seat to transmit the thrust. When the wind blows from the opposite direction compression is induced in the top plate and tension at the beam seat. Therefore, the unwelded portion of the top plate must be designed to act as a column to transmit the compression and the horizontal element of the seat must be welded to the column to resist the tensile force.

A welded top plate connection that develops 65 ft-kips wind moment for the beam used in the comparison and similar to the one shown in Fig. 8 costs $0.81 less than the riveted connection shown in Fig. 2(a) and eliminates other forms of wind bracing as well.

As welding makes possible the elimination of all labor and material for bracing

Fig. 10 Butt-plate type of splice must be used where column sections change size but is often used for columns of the same size as shown in photograph

and its connection by the substitution of a comparatively light top plate for a flexible top angle at a beam-to-column connection with only slightly more labor than that ordinarily required for the beam-to-column connection, designers should not hesitate to adopt welding to utilize this very economical type of wind connection. Of course, this alternate method applies to simple framing only. Other considerations may indicate the adoption of rigid framing for even greater economy by welding.

COLUMN SPLICES

The selection of welded column splices depends largely upon the arrangement of the column and the conditon of loading. Figure 9 shows a butt-welded column splice requiring a minimum of labor and material. Except for electrodes, two pairs of small clip angles, shop welded to both columns at diagonally opposite toes and on the inside of the flanges, constitute all of the connection material. The clips serve for erection purposes only. The splice is shop prepared by beveling the outside of the flanges of the upper column and field welding the usually incomplete penetration butt joint formed. The load was primarily axial on the column illustrated and the depth of bevel was $^1/_2$ in. The depth of bevel for other column splices in the same building was one-half the thickness of the flange or $^1/_2$ in., whichever was smaller.

When considerable bending moment occurs at a column splice, the butt-welded splice can adequately handle the resulting tensile and shearing stresses. This is done by increasing the depth of bevel as required until the joint approaches 100% penetration. In such cases a $^1/_8$-in. minimum shoulder must remain after beveling to provide some milled bearing surface for the flanges to facilitate plumbing before welding. The web of the column is not beveled in any case, but a sealing bead may be applied to each side of the web if desired.

The butt-welded splice can be used only when the columns are of the same nominal depth. Figure 10 illustrates a welded butt-plate type of column splice. The shallow depth of the beam framing into the web of the column below the splice permitted shop welding of the plate to the lower column and downhand field welding of the upper column to the plate. If a butt-plate on the lower column will interfere with the seating of a beam during erection, then the plate must be shop welded to the upper column, requiring more expensive overhead field welding. When the columns joined are of the same nominal depth, the butt-plate is usually $^3/_4$ in. thick, but it is made 2 in. thick for columns differing by 2 in. or so in depth.

A comparison indicates the butt-welded splice will cost $1.11 less for labor and material than the butt-plate type of splice for 10-in. WF columns where the field welding is downhand for the butt-plate type. In comparison with a column splice utilizing connection plates shop and field welded to the flanges (not illustrated), the butt-welded splice will save $3.82. Over a similar riveted connection the butt-welded splice saves $7.02.

SUMMARY AND CONCLUSION

Comparisons have been made of the relative merits of various connections and the three types of framing permitted for building construction. The fact that semirigid framing potentially offers the greatest possible economy was brought out but not emphasized because its use is somewhat limited by present stipulations in building codes. The comparisons indicate a building designed with fully rigid connections, butt welding beam flanges directly to columns, and using columns of the same nominal size, in so far as practicable, for the entire height of the structure to utilize a butt-welded column splice, will be most economical at the present time.

The comparisons also indicate a combination of standardized framing angles, built-up T-seats, butt-welded column splices and plastic wind connection plates will produce the most economical simply framed structures.

The owner seldom has the knowledge or experience to specify one type of construction or another; therefore, he depends upon his architect and structural engineer to provide him with the best possible structure at the least possible cost. To faithfully fulfill this trust the architect and engineer must consider all competitive methods, materials and processes and be thoroughly informed as to their relative merits.

Riveting is a competitor of welding, reinforced concrete is a competitor of steel, yet in many respects one complements the other. The various industries in the construction field continually strive to improve their competitive positions by research and development work and professional engineers play a most important role in this respect. Many steel fabricators now advocate welding in lieu of riveting, research workers and engineers are completing studies of plastic design, the

concrete people are rapidly developing prestressed concrete and scientists are already investigating prestressed welded steel structures.[4] The primary object of all this intensive effort is to reduce building costs, but unless the designer is kept fully informed of results, and knowledge of cost is certainly a major result of any development, he cannot intelligently specify one method, material or process in preference to another. The purpose of this paper will be served if it encourages a designer to specify welding.

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Surface Alloying in Brazing and Related Techniques

Some fundamental considerations of surface alloying in brazing and bonding

by Dr. Robert Humphrey and Rene D. Wasserman

INTRODUCTION

THE study of the union of two metals is a fascinating subject which has received considerable attention. So far no completely satisfactory explanation has been given of the mechanism underlying bonding.

The mechanism of metallic bonding is primarily related to attractive forces between atoms. These forces are also associated with the electron configuration of the metals involved. A case in point is furnished by Johansson blocks. These blocks are lapped to flatness up to 0.000002 in. When "wrung" together they possess a cohesiveness of more than 30 times that which can be ascribed to atmospheric pressure. Figure 1 illustrates the cohesiveness of a combination of blocks which had sustained a weight of 220 lbs. Computation shows that the atmospheric pressure could not have contributed more than 6.6 lb. This experiment illustrates that two metallic surfaces may be attached to each other by attractive forces.

Metallic bonding is further aided by diffusion, which is the movement of atoms within a solution—usually from regions of high concentration toward regions of low concentration. The rate of diffusion in metals is normally considered to be a function of pressure, temperature and concentration.

If the temperature of a metal is raised, the diffusion rates are increased and ability to form metallic bonds is facilitated as, for example, in the forging together of two bars of iron at elevated temperatures.

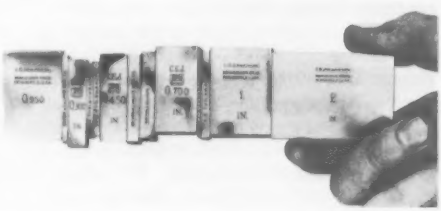

Fig. 1 Illustrating the cohesive qualities of a combination of Johansson Gage Blocks. "Wrung" together they have sustained a weight of 220 lb, of which atmospheric pressure could have contributed no more than 6.6 lb.

There are many gaps and defects in the atomic structure. They are more than sufficient to accommodate the passage of many other moving atoms. Diffusion through the lattice occurs in three ways, by (*a*) substitution of atoms, (*b*) interstitial diffusion and (*c*) vacancy diffusion. These conditions are shown in Fig. 2.

Figure 2*a* illustrates the substitutional type of diffusion in a simple cubic system. An A atom passes through the lattice and exchanges places with a B atom.

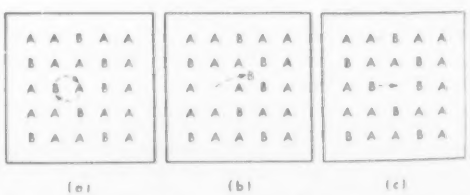

Fig. 2 Illustrating the movement of atoms through the space lattices: (a) Substitution, (b) interstitial, (c) vacancy

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The nature of interstitial diffusion is evident from Fig. 2*b*.

When a lattice has a vacancy site, i.e., a position not being occupied by an atom, it may become filled by another atom. Such a process is termed "Vacancy Diffusion" and is illustrated in Fig. 2*c* showing a B atom which can move into a vacant location within the lattice. After atom B has moved into the vacant spot, any adjacent atom may now move to fill the vacancy left by the migrating atom B.

BRAZING

Bonding two or more metals together with a filler metal that has a melting point above 800° F, but lower than those of the metals being joined, is termed "brazing." Most brazing processes are conducted in the open with the aid of satisfactory fluxing methods, or in a controlled gaseous atmosphere. "Wetting" is one of the prerequisites for insuring strong metallic bonds. The alloying characteristics of the metals involved appear to determine their wetting qualities, or the ability to spread. Mercury on iron shows no perceptible diffusion. However, when it is in contact with copper it may penetrate and show considerable alloying.

Molten lead does not "wet" or alloy with iron. Tin, on the other hand, does wet readily with iron. Solders composed of both tin and lead wet or flow freely and form relatively strong metallic bonds with iron.

In the case of mercury we have a liquid where the atoms have maximum thermal agitation and mobility when compared to the solid state. Why then is *metallic bonding* not possible? How can we classify our metals from observed facts into those that bond readily, those that bond with great difficulty and others which do not bond at all? The constitutional or equilibrium diagrams help us. These diagrams express the relationship between phases under equilibrium or near equilibrium conditions. In these diagrams temperature is plotted against composition, keeping vapor pressure as a constant of one atmosphere.

In binary alloys, there are four basic types of solid phases:

1. Commercially pure metals.
2. Primary solid solutions.
3. Intermetallic compounds.
4. Secondary or intermediate solid solutions.

In Figs. 3 and 4 we have the silver-copper and copper-nickel equilibrium diagrams which illustrate primary and complete solid solution types of diagrams respectively. The characteristics of the solid solutions are similar to those of the pure metals.

The silver-copper diagram illustrates a primary or terminal type of solid solution. The primary solid solution occupies only a portion of the diagram and indicates that complete miscibility does not occur.

Complete miscibility of solute and solvent atoms do occur in the copper-nickel diagram. It would be safe to assume that more efficient *metallic bonding* is possible between Cu and Ni than between Cu and Ag.

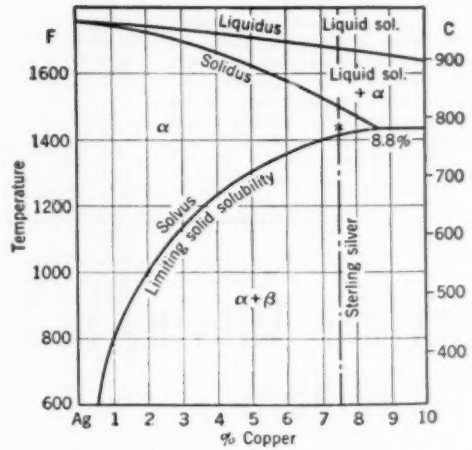


Fig. 3 A portion of the silver copper equilibrium diagram illustrating the primary solid solution of copper in silver

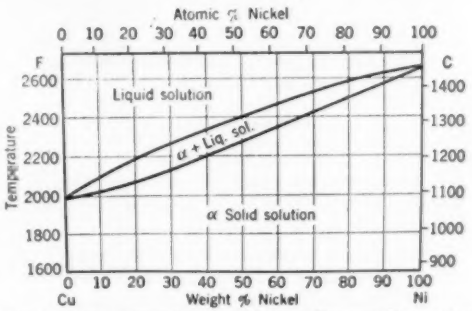


Fig. 4 The copper nickel equilibrium diagram illustrating the complete solid solubility of copper and nickel

Commercially pure metals and terminal solid solutions will not be discussed as they represent limiting cases of the above.

Intermetallic compounds have definite chemical compositions and properties. They melt or decompose at definite temperatures into either a single phase or a mixture of phases.

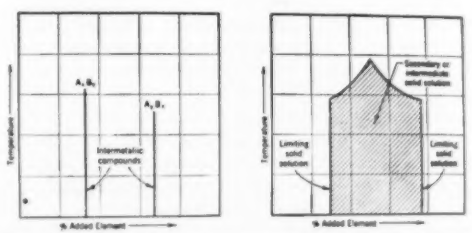


Fig. 5 (Left) Schematic representation of two intermetallic compounds on a binary constitutional diagram. (Right) Schematic representation of a secondary or intermediate solid solution on a binary constitutional diagram

In a constitutional diagram, an intermetallic compound is usually indicated by a vertical line (see Fig. 5). Intermediate solid solutions form from intermetallic compounds. As a rule, these phases are hard and brittle.

Binary alloys may possess any two of the above-mentioned phases as stable mixtures and may coexist in equilibrium at the right temperature.

THE EUTECTIC

Partial solubility of two constituents is illustrated in the diagram in Fig. 6 of the silver-copper system. This type of alloy system is of ***extreme importance*** to the ***brazing and welding industry***. The cooling curves for pure silver and copper are shown as *a* and *i* respectively.

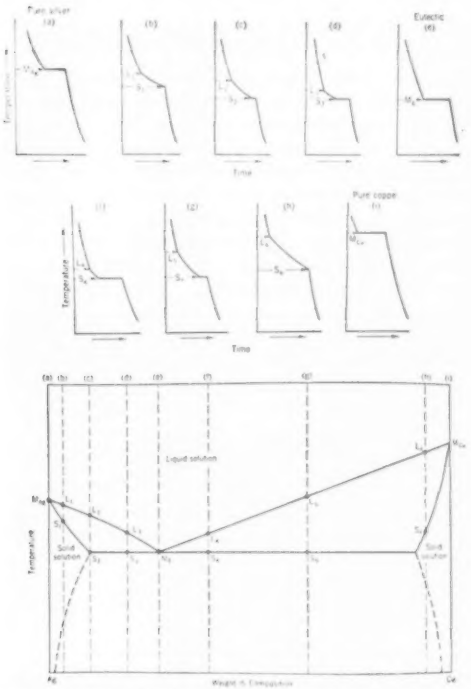


Fig. 6 (Above) Idealized cooling curves for a series of silver copper alloys. (Below) The silver copper binary diagram, as plotted from the cooling curve data above, and illustrating complete liquid solubility and partial solid solubility

Upon passing from liquid to solid the cooling curve of these pure metals is arrested as illustrated in Fig. 6*a*. The other cooling curves, with the exception of the composition designated by *e*, show gradual changes in the slope of the curves where both liquid and solid coexist and solidification passes through a range of temperatures.

The silver-copper system exhibits these types of curves. On the silver side of the diagram, the cooling

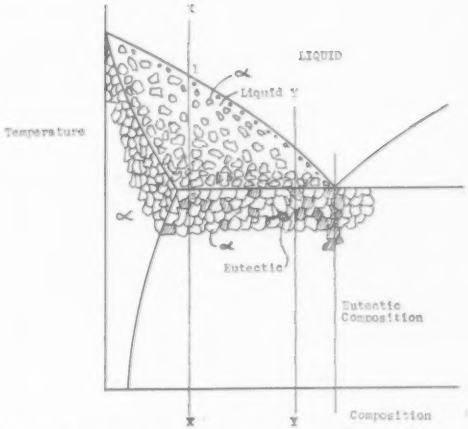


Fig. 7 Graphic representation of barrier offered to diffusion by physical state

curves are similar to those in the Cu-Ni system where complete solubility in both the liquid and solid states occurs. This characteristic property is also observed where Cu in the Ag-Cu system is the predominant solvent. In this system, it is found that when an alloy of *71.5% Ag–28.5% Cu* is reached (composition *e*), the shape of the cooling curve simulates the characteristics of a pure metal, or an intermetallic compound. The remarkable property of this composition is that it solidifies at a constant temperature of 780° C, *and furthermore*, it has the lowest freezing point of any of the family of alloys in this system.

Further consideration of this alloy shows the following characteristics:

1. It is not a solid solution since it melts at a particular temperature.
2. It is composed of two mechanically mixed distinct phases; therefore, it cannot be an intermetallic compound.
3. At high magnification, the structures that are resolvable appear to be uniform.

Alloys of the above characteristics are considered to be separate entities and are termed "eutectics."

An alloy in a metallic system which possesses the lowest transforming point on passing from liquid to solid is called "eutectic."

It may be better expressed by the equilibrium relation:

$$\text{Liquid} \left\{ \begin{matrix} \text{cooling} \\ \longrightarrow \\ \longleftarrow \\ \text{heating} \end{matrix} \right\} \text{Solid}$$

In brazing or welding, excessively long periods at high temperatures may lead to serious distortion, excessive grain growth, porosity, burning, etc. Eutectic compositions tend to minimize these objectionable physical properties because they solidify quickly. On solidifying, they do not pass through a range of tem-

peratures where both solid crystals and liquid phases occur simultaneously prior to complete freezing. However, the use of pure eutectic compositions is often not practical since excessive quenching strains may result in cracking. To minimize these defects, alloy compositions close to the eutectic composition are used to provide improved brazing properties. These alloys, due to their low melting point, require much less heat input during a bonding operation; thus they far surpass other alloy mixtures which exhibit undesirable postbonding results.

Graphically we can visualize this condition by reference to Fig. 7. Take the alloy compositions arbitrarily listed as *X-X*, and *Y-Y*. We see that the alloy freezing along line *X-X* follows a long-temperature range from *l* to *s*. In this region *l-s* we have a mushy alloy condition wherein the maximum thermal agitation of the atoms is retarded. For *Y-Y*, an alloy composition close to the eutectic, a similar state exists, but at a *much lower melting point*. The surface of the solid metal being welded or brazed is in direct contact with this metal whose atoms are in the high state of agitation, a condition which is optimum for both surface diffusion and penetration.

SURFACE CONDITION

In metallic bonding the preparation of the surfaces is of major importance. Commercial metals are usually covered with films of varying densities and compositions. Chemical analysis has shown that these films consist of oxides and, in addition, may contain other materials such as sulfides, sulfates and carbonates. Schematically we can then represent such metals in the manner sketched in Fig. 8. The oxide film acts as a barrier in brazing and must be removed either chemically or mechanically to permit satisfactory metallic bonding.

There are three major methods of cleaning metal surfaces prior to brazing: (1) fluxing with salts that dissolve and absorb the oxide films; (2) reducing the oxide films with reducing gases; (3) immersing in molten baths that combine both absorption of the oxides and an oxide-reducing action. To do a satisfactory job, the flux must possess proper physical as well as chemical properties. The flux should blanket the brazing alloy upon liquefying, thereby minimizing oxidation. It should be sufficiently viscous to serve as a blanket and not run off or spread too thin. It should also be clear to allow the operator to examine his work. To possess this latter property is convenient but not absolutely essential.

A good flux should be durable, water soluble and capable of being deposited by brushing, dipping or spraying methods. In addition, it should wet the metallic surfaces and should not streak or spread in clumps.

Oxides and other contaminations on the base metal and brazing alloy should be dissolved and absorbed by the molten flux. The unsaturated portion of the flux should be adequate to permit thorough metallic cleaning during the brazing operations. A clean surface can also be obtained by reducing the oxides to the metallic condition.

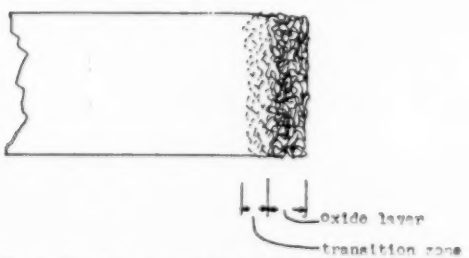


Fig. 8 Graphic representation of oxide layer on a metallic surface

NEW FLUXES WITH DETERGENTS

Certain chemical compounds possess the properties of reducing the surface tension of liquids, enabling them to wet completely foreign surfaces. They are called "detergents." Similarly, certain activating compounds are often added to brazing fluxes enabling them to produce similar effects. A well-balanced flux usually contains fluorides, chlorides, borates and small quantities of these activators. The flux should have the following characteristics: (1) produce no excessive runoff, (2) melt at temperatures below the melting point of the brazing alloy, (3) reduce or dissolve the oxide films, (4) wet completely both the base metal and the brazing alloy, (5) protect the base metal against oxidation during heating, (6) activate the surface prior to brazing, (7) be displaced by the brazing alloy when liquid.

ACTIVATION OF SURFACE

Activation of a metallic surface is most important for good metallic bonding. We have seen how two Johanssen blocks, when brought into intimate contact, have exceptional holding power. We may term each block as having its surface activated to permit strong metallic forces to be brought into action. A good flux behaves similarly. As the atoms at the surface of a base metal are thermally agitated by heat all foreign matter is removed by means of the molten flux. Thus the virgin metal becomes exposed, permits the molten brazing alloy to diffuse into the base metal. This results in the formation of strong bonds.

NEW OBSERVATIONS IN BONDING

Strong metallic bonds are possible when a rapidly diffusible metallic layer is deposited on the base metal prior to brazing. The ensuing accelerated diffusion results in strong bonds. These intermediate layers of monatomic metal thickness may come from the filler rod or from the flux mixture itself. Details relative to this method are recorded in Patent No. 2583163 covering deposition from the filler rod.

The flux may contain metal ions which can be reduced and deposits upon the base metal. The deposition occurs at temperatures far below the melting points of the brazing materials. The deposited layer has the opportunity to fuse into the base metals. This is especially true when the deposited ions are low melting elements, easily alloyable to the base metal. In this manner, brazing takes place readily because: (1) The diffusion occurs to atomically clean surfaces having high surface mobilities; (2) the two surfaces of the base metals alloy with the liquid brazing material by means of a low melting interface. These metal ions are important brazing carriers and the layer deposited follows the law governing the electromotive-force series.

CONCLUSION

In summation, we find that strong metallic bonds depend upon the degree of diffusibility of the metals, which in turn is a function of pressure, temperature and concentration.

The surfaces of even compatible metals must be treated before they can be brought into a condition favoring diffusion and the formation of strong metallic bonds. This treatment can be accomplished by mechanical means, specific atmospheric treatment, chemical reducing action or by the use of scientifically designed fluxes.

The knowledge incorporated herewith has been utilized into devising and making new superior brazing alloys and fluxes. An example of this is given in the Appendix.

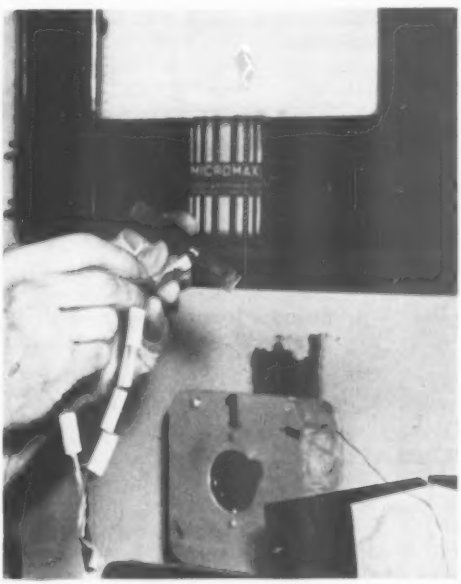


Fig. 9 Procedure illustrating how the thermocouples were inserted into the parent metal of the weld sample. The hot junction of the thermocouple was placed in contact with the uppermost surface of the drilled hole; that is, within 0.002 of the top surface, and then connected to a Micromax temperature recorder. The close contact between the thermocouple within the parent metal was the only means to hold the thermocouple in place

Fig. 10 Example of weld metal deposit applied with the conventional rod and flux

Fig. 11 Example of weld metal applied with alloy BCuZn 7 rod with Eutector Flux

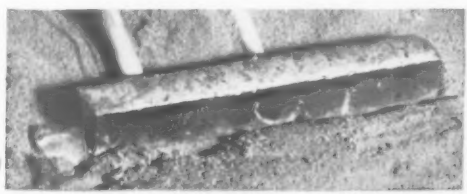


Fig. 12 Completed weld deposit with BCuZn 7 alloy with Eutector Flux indicating the free-flowing characteristics as temperature recorded 1300° F.

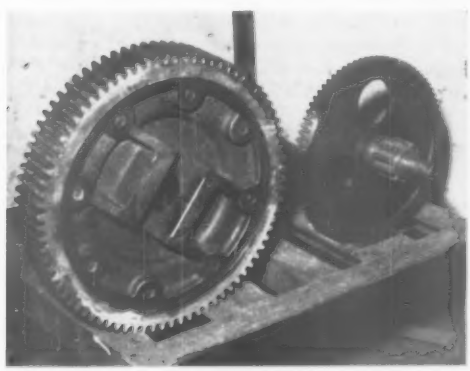


Fig. 13 Railroad retarder mechanism gear built up with BCuZn 7 and machined

Fig. 14 — Retarder mechanism gear box repaired with BCuZn 7 and machined

Fig. 15 Streetcar trolley shoes built up with BCuZn 7

Appendix

A series of experiments have been conducted to determine the temperatures reached by the base metal in depositing a BCuZn 7 filler metal. The experimental procedure was designed to demonstrate the effects of flux, composition of non ferrous rod and technique of deposition on the maximum temperature attained by the base metal in depositing a sound, dense filler metal. A conventional manganese-bronze rod using a conventional flux and deposited in a conventional manner was compared to the EutecRod BCuZn 7 with the corresponding Eutector flux for depositing the alloy.

The deposits were made on strips of mild steel, $^1/_2$ x $^1/_2$ x 4 in., which were machined to accommodate thermocouples placed close to the surface onto which the weld was deposited, as shown in Fig. 9.

The thermocouples were inserted into the weld sample in such a manner as to place the hot junction of the thermocouple in good contact with the uppermost surface of the drilled hole, i.e., within 0.002-in. of the top surface. The cold junction of the thermocouples were connected to a Micromax temperature recorder which was calibrated previous to the test runs, so that all temperature readings were completely automatic, accurate within ± 10° F, and permanently recorded. Figure 10 illustrates the alloy applied with a conventional rod and flux. Surface alloying was retarded. Figure 11 shows BCuZn 7 alloy applied with Eutector Flux having free-flowing characteristics. Figure 12 shows the completed test specimen.

Fig. 16 Glass manufacturing roll which had been warped by heat treatment. (A) Rolled overlaid with BCuZn 7 without distortion or warping. (B) Overlaid roll after machining to concentric dimensions

The following table of results is compiled from an average of data taken during these tests.

Table of Results

	EutecRod BCuZn 7, °F
Liquidus	1710
Solidus	1685
Maximum temperature reached by base metal upon filler deposition	1350

A few typical applications of EutecRod BCuZn 7 are illustrated in Figs. 13–16.

Acknowledgment

The authors wish to thank W. Kriewall and F. Polizanno for their assistance in preparing the brazing tests discussed in the Appendix.

Magnetic Force Welding

A machine has been developed which uses the magnetic force of the welding current to produce rapidly rising pressure on the work adjustable in magnitude and synchronized with the welding current

by Myron Zucker and Guy Cubitt-Smith

Abstract

A new type of spot and projection welder has been in service for over two years, producing millions of difficult welds with great reliability.

This machine uses the magnetic force of the welding current to produce rapidly rising pressure on the work. The force is adjustable in magnitude and is always perfectly synchronized with the welding current.

Developed for a specific job of welding high-conductivity metals such as copper and silver, the machine shows promise of improving technique with other metals. It is essential to know the machine characteristics in order to make good use of the new principle in new welding applications.

This article describes the magnetic-force welder, reports on force-time and related tests, and tells the results of some laboratory and production welding of copper, silver, brass, tungsten, aluminum and steel.

INTRODUCTION

A BATTERY of spot welders whose electrode force comes from the welding current[1] has been producing a variety of welds for over two years, with much greater consistency than had been previously obtainable with standard welders.

This success stimulated an investigation of the special characteristics of this type of machine, with a view to their possible application to a wider range of welds—particularly the difficult ones.

The major advance in these machines lies in the rapid, synchronized application of electrode force which makes it practicable to weld on a rising electrode force wave. This, in effect, gives an exceptionally fast follow-up. The importance of this factor has been increasingly recognized on research[2-4] and in practice through recent years. Fast follow-up improves weld quality, decreases expulsion, arcing and tip pickup.

The ability to apply a rapidly rising electrode force also allows the use of lower initial electrode force. This has two advantages: Increased heat from a given current because of higher contact resistance; and concentrated heat from projections on materials such as soft aluminum or magnesium that would collapse under the electrode force of conventional welders.

The successful operation of the magnetic force welder depends upon the synchronized timing of the magnetic electrode force with the welding current. Unfortunately, programmed pneumatic systems of present design are not capable of meeting these requirements due to the problems of inertia, air flow and erratic valving—whereas, in the magnetic force welder, the synchronizing is inherent.

Setups on a machine with mechanical action as radically different as this one cannot be made casually. To take full advantage of its versatility and consistency, some attempt must be made to match the welding requirements to the machine characteristics. It is necessary to know, for instance, the shape of the pressure wave as each machine setting is changed.

Therefore, before extensive work was done on welding, the characteristics of the machine itself were obtained, in curve form.

This report gives those curves.

Then it shows how the curves can be used as a guide in setting up for welding.

Finally, it produces some welding results on soft aluminum wire, projection-welded copper, projection-welded tungsten to steel, and spot-welded steel.

DESCRIPTION OF MACHINE

The welder on which these tests were made was an outgrowth of the 30-kva magnetic force machine previously reported by Klingeman and Kruer in the JOURNAL OF THE AMERICAN WELDING SOCIETY.[1]

This new machine is an experimental unit, furnished by courtesy of Precision Welder and Flexopress Corp. It is basically a size 1A Aircraft spot welder, 100-kva three-phase, 36-in. throat machine, No. 150705, illustrated in Fig. 1.

The ram is Precision ternalloy, ball-bearing mounted. The upper arm is the standard integrally formed with the body of the machine. The lower arm has the com-

Myron Zucker is connected with the Myron Zucker Engineering Co. and is representative for Precision Welder and Flexopress Corp. and **Guy Cubitt-Smith** is English technical consultant and formerly was with the Precision Welder and Flexopress Corp.

Paper presented before the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Feb. 15, 1953.

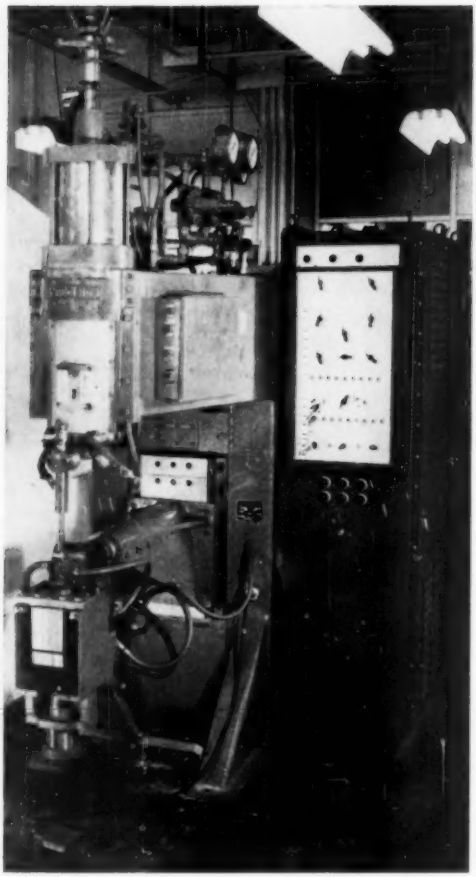


Fig. 1 Experimental magnetic force welder

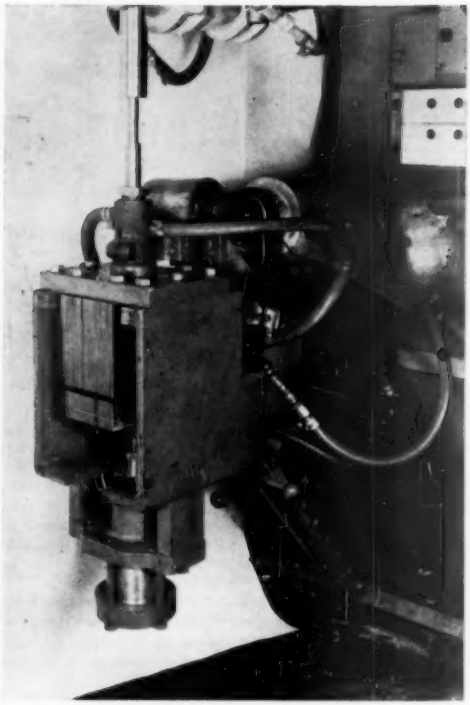


Fig. 2 Magnetic force unit

The single-turn coil is within the casing, connected between the heavy lead-in conductors to one of which the shunting shims are bolted (to right). The laminated steel magnet iron is seen at the left. The upper, larger portion is one leg on an inverted U stamping. The other leg enters the magnet coil. Lower laminations are the armature or clapper that is pulled toward the open ends of the U by magnetic force in turn transmitting this force to the electrode shaft. The shaft is guided by ball bearings above and by the auxiliary air cylinder below.

mon extensible horn with spot-welding electrode holder and No. 2MT tips.

Magnetic action is brought to the machine by removing the standard lower horn and inserting a special one shown in Figs. 1 and 2. On this is mounted a one-turn coil carrying the full welding current, except for the portion that might be diverted by an adjustable shunt. The coil surrounds one leg of an inverted U-shape iron core. Magnetic force is developed across the air gaps between the ends of this core and an iron armature or "clapper" attached to the electrode shaft. These gaps do not close.

There are two interchangeable sets of iron: one of 26-gage silicon transformer iron; the other of solid cold-rolled mild steel.

There is also a small air cylinder at the bottom of this unit, furnishing the initial force under steady but adjustable air pressure.

Electronic controls are standard Weltronic frequency-converter, including forge delay and tailing heat. The forging element was used on the frequency-converter tests, but could not be brought into play with the magnetic force.

From this brief description it can be seen that the value of force applied to the electrodes is subject to seven adjustments. Some are major, others not so important. Their names and ranges are given in Table 1.

Table 1—Adjustments and Design Features Governing Magnetic Force on Welder Being Tested

Variable	*Range of values or choices*
Mechanical:	
Air gap	0.035 to 0.125 in.
Initial pressure	0 to 100 psi in 3-in. cylinder. (Important when welding, but not when obtaining short-circuit force characteristics)
Magnetic:	
Core	Laminated soft iron; solid CR steel
Shunt	None to 9/16-in. thick copper bar
Electrical:	
Heat setting	30 to 100%
Weld duration	1 1/2 to 5 1/2 cycles (60-cycle basis)
Operating sequence on successive welds	Alternate-polarity pulses; continuously positive; continuously negative.

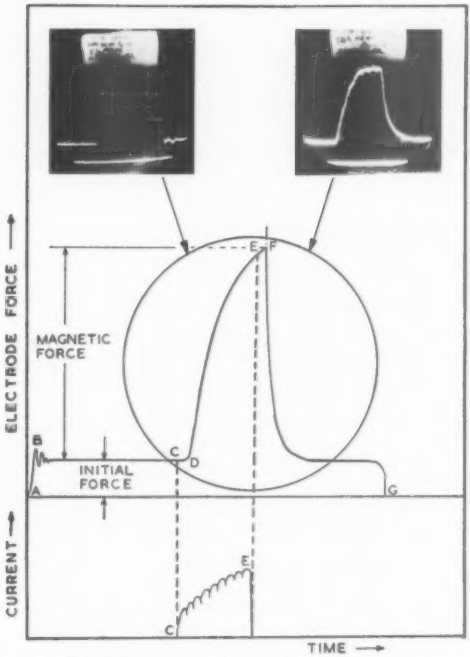


Fig. 3 Schematic diagram showing the sequence of operation and typical oscillograms

MACHINE CHARACTERISTICS

Tests were aimed primarily at the electromagnetic action that is the new feature of this welder—after enough work had been done to reassure us that the electrical control and power circuits were not greatly affected by the magnet.

The shapes of the force-waves were found from cathode-ray oscillograms with no work between the welder electrodes. Details of test methods and precautions are given in Appendices 1 and 2.

Oscillograms

Figure 3 shows the complete weld cycle and illustrates the portion shown in typical oscillograms. Identifying points are: *A*, initiating switch to welder controls is closed. *B*, impact of the electrodes under air pressure. This transient pressure was used to trigger the oscilloscope, so that only the magnetic force portion of the picture appeared on the film. *BC*, squeeze time. *C*, current initiated. *CD*, magnetic pressure delay, the short interval between initiation of current and of magnetic force build-up. *CE*, weld duration, lasting $1^1/_2$ to $5^1/_2$ cycles. On this machine, current is rectified from 3-phase power. It increases gradually to its maximum value, and has appreciable ripple. *F*, pressure begins to drop. *EG*, hold time set on electronic controller: The air pressure is released and weld cycle completed.

Table 2—Conditions and Settings for Figs. 4–16

Current always in negative pulses (plus alternate in Fig. 9). Pulse duration $5^1/_2$ cycles on all curves (plus $1^1/_2$ to $4^1/_2$, in Fig. 4)

	Force-times curves								
Figs.→	*4*	*5*	*6*	*7L*	*7R*	*8AL*	*8AR*	*9*	*10*
Magnet									
Solid	x	x	x	x		x	x	x	
Lam.					x	x	x		x
Air gap, in.									
0.035	x		x						
0.075			x			x	x	x	x
0.100					x				
0.125		x	x	x					
Shunt,* in.									
None	x	x	x	x	x	x		x	x
6–12				x	x		x		
$^1/_4$				x					
Heat, %									
30		x			x		x		x
50	x	x	x	x			x	x	x
80		x				x			x
Miscellaneous	$1^1/_2$ to $5^1/_2$							Alt. Neg.	

	Derived				*Supplementary*	
Figs.→	*11*	*12*	*13*	*14*	*15*	*16*
Magnet						
Solid	x	x	x	x	x	x
Lam.	x			x		
Air gap, in.						
0.035		x	x	x		
0.075		x		x	x	x
0.100		x		x		
0.125	x	x	x	x		
Shunt,* in						
None	x	x	x	x	x	x
6–12			x	x		
$^1/_4$			x	x		
Heat, %						
50						
80						
	Peak force plotted against heat					

* 6–12 indicates number of 0.010-in. shims used for shunt; $^1/_4$ in. was an aggregation of shims and bars totaling about $^1/_4$ in.

The encircled section of Fig. 3 shows a typical oscillogram. All the force-time curves in this paper are selected from more than 75 oscillograms. The curves usually show average forces, neglecting ripple unless it has special significance.

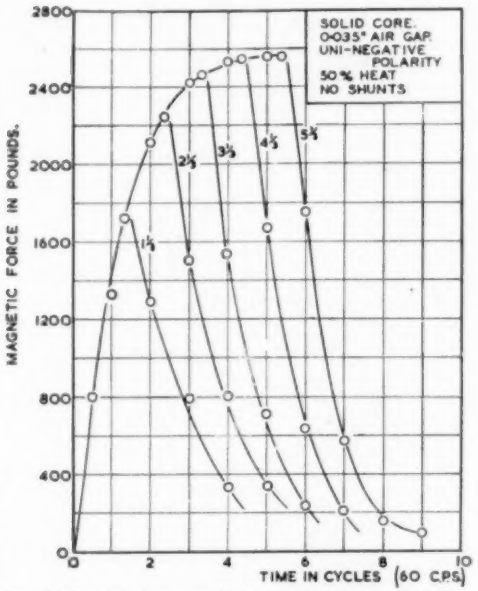


Fig. 4 Family of curves showing the effect of weld time

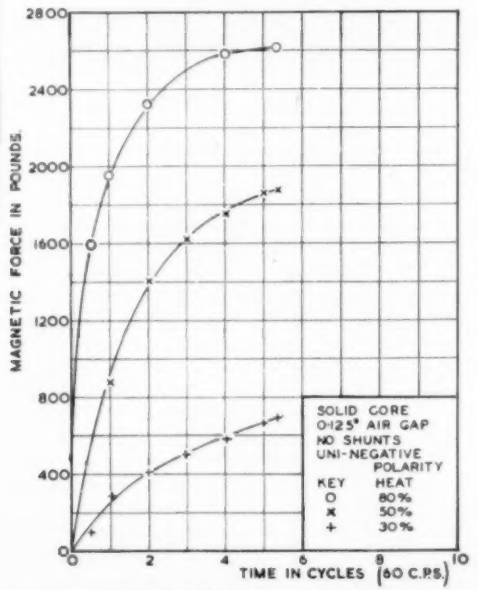


Fig. 5 Effect of heat setting

Variables

The effect of varying any of the factors listed in Table 1 is shown in the "families" of curves in Fig. 4—16. To clarify this series, we list in Table 2 the values of the adjustments that were fixed for each figure, as well as those (boxed in) that were varied to get the families of curves.

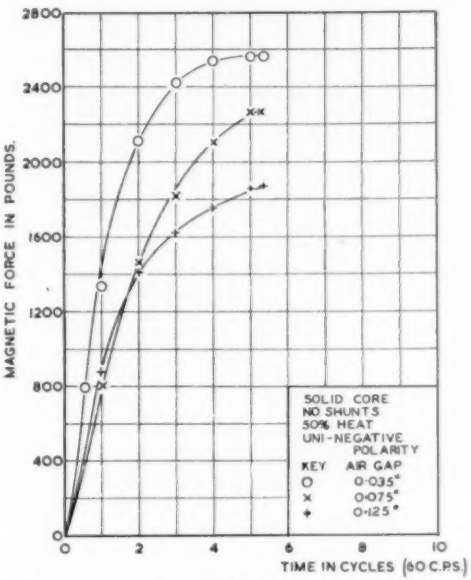


Fig. 6 Effect of air gap

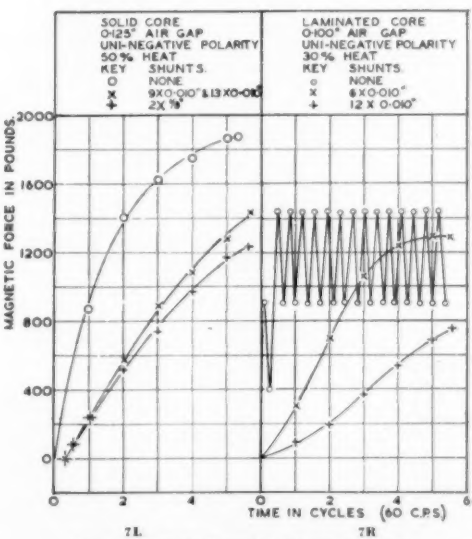


Fig. 7 Effect of shunting the magnet coil

Form of Presentation

As shown in Table 2, the curves are grouped as follows:

(*A*) Representative *force-time curves* are plotted (Figs. 4–10) from selected tests to show trends of the force waves with no work in place.

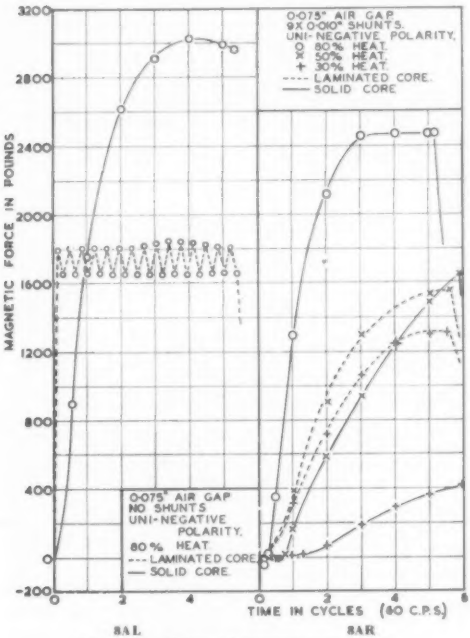


Fig. 8 (A) Effect of magnet core design

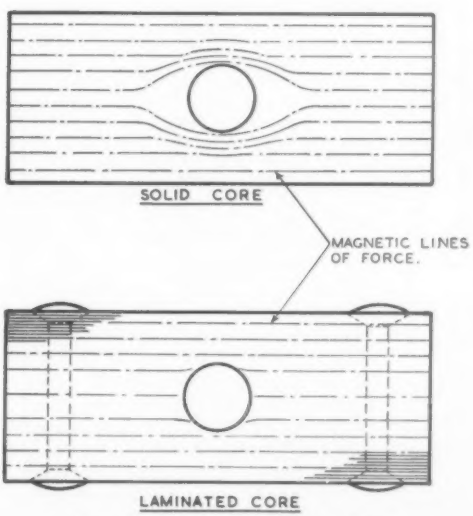


Fig. 8 (B) Effect of magnet core design

(*B*) From the force-time curves are *derived curves*, showing (1) peak force vs. heat setting and air gap in

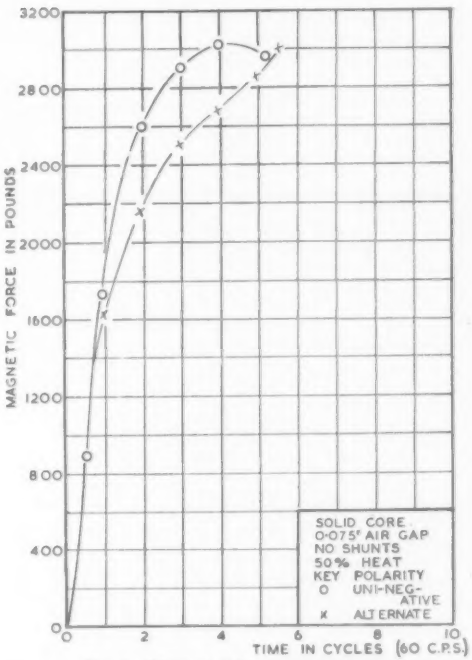


Fig. 9 Effect of weld current polarity

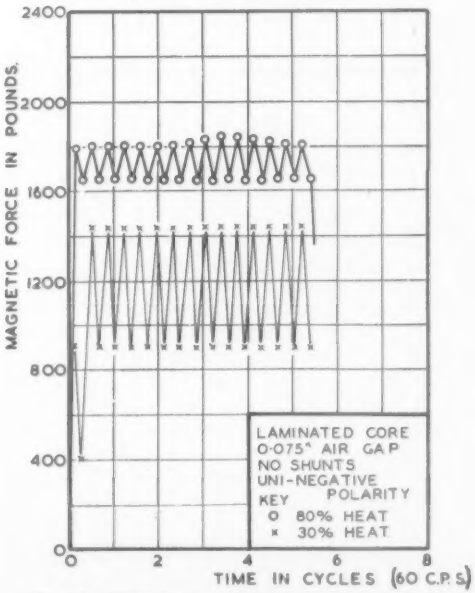


Fig. 10 Effect of phase shift heat control on ripple

the magnet (Figs. 11–12); (2) values indicating the shape of the force-time curves: time to start magnetic force, Fig. 13; and maximum rate of rise, Fig. 14.

(*C*) *Supplementary force-time curves* showing (1) tailing-force patterns, Fig. 15 and (2) force-time curves with work in place, Fig. 16.

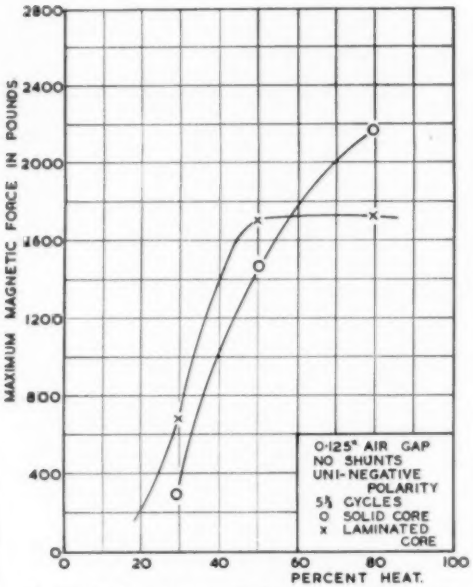


Fig. 11 Maximum magnetic force versus percent heat

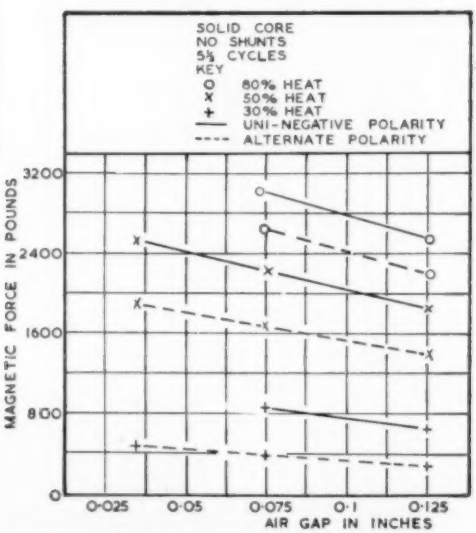


Fig. 12 Maximum magnetic force versus air gap

Force-Time Curves

The first set of oscillograms, plotted in Fig. 4, was made with weld duration as the variable. It shows that the longer weld time produces a force that is simply an extension of the curves for the shorter times. This was checked with many other machine settings. The consistency was so great that thereafter $5^1/_3$ cycles

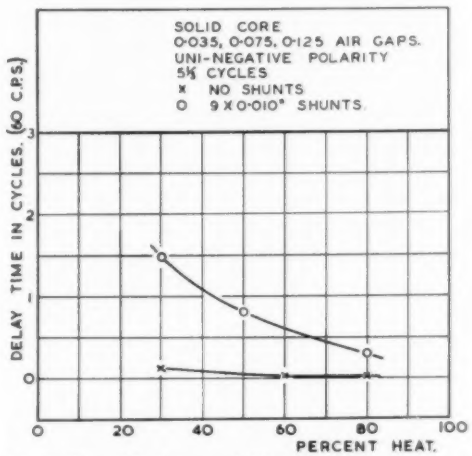


Fig. 13 Time delay between initiation of current and start of magnetic force build-up

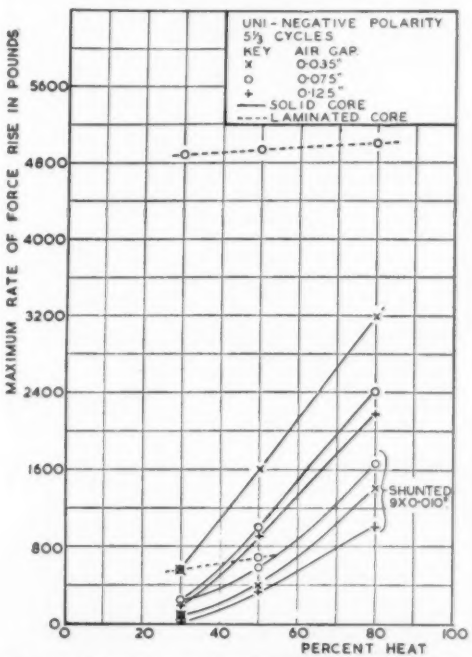


Fig. 14 Maximum rate of magnetic force versus percent heat

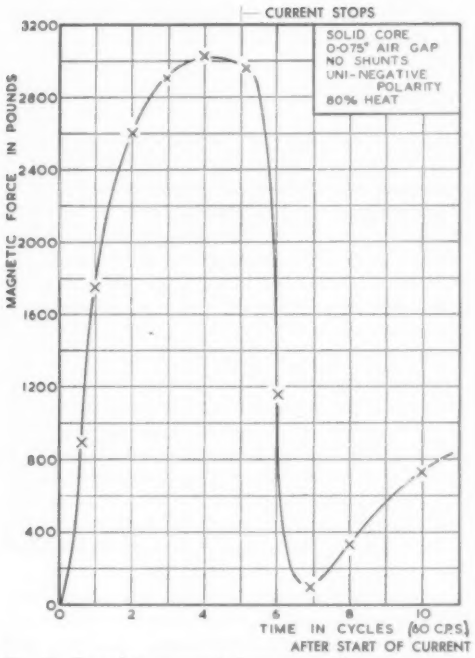


Fig. 15 Complete magnetic force curve illustrating rapid decay of force

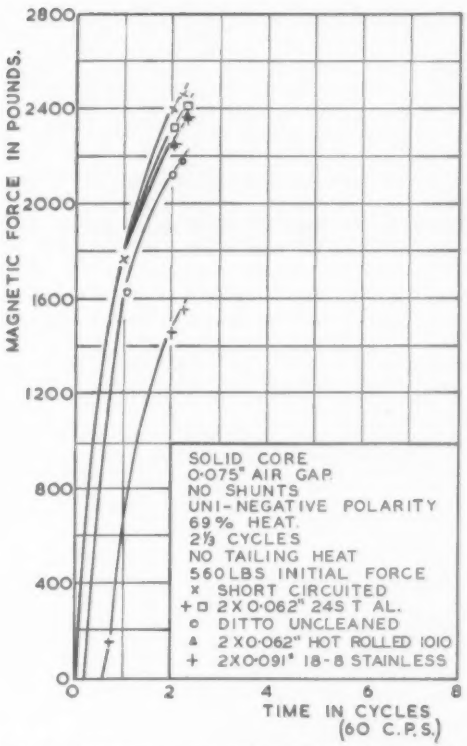


Fig. 16 Force-time curves with workpieces between the electrodes

weld time was used to establish the force pattern for all other variables.

Figure 5 shows the effect of heat setting (current). Higher heats increase forces, as expected. Each curve is a reproduction of the current-time relation, rising rapidly at first and then slowing down as the current approaches its steady-state value. In some cases (e.g., Fig. 10), the force wave has a flat top because of saturation of electromagnet.

Figure 6 shows that the air gap between the armature and the fixed iron in the magnet has only a moderate effect on force, even in the solid-core case illustrated. The increase air gap causes slight decrease in rate of rise and in maximum force. The gap has little or no effect on laminated magnet.

Both the electrode force and the electromagnetic "kick" on the welder conductors open the throat slightly. This decreases the air gap during the weld and tends to increase the electrode force at a time when this is desirable.

The initial air gap must be made sufficiently large so that it is not closed by this action plus the setdown of the weld.

Shunts, Fig. 7, can affect the curves in three ways:

1. Delay the start of force rise. This is because of the electrical relations between shunt and coil that make the initial current go through the shunt, and build up later in the magnet.

2. Decrease the rate of rise of force, for the same reason.

3. Cut down the peak force by diverting part of the steady-state current from the magnet. This effect will be in proportion to the resistance of coil and shunt, if the current flows for a long enough time.

The importance of these effects depends on other machine conditions. For instance, shunting of the solid-core magnet (Fig. 7*L*) delays and decreases rate of rise and maximum force. But shunting the laminated magnet (Fig. 7*R*) introduces a slight delay (which is often desirable), decreases rate of rise radically so that it is about the same as for solid-core values, but affects maximum force relatively little because the magnetic core is saturated even at the decreased coil-current (see "magnet core" discussion that follows).

Figure 8 shows the magnet core. One of the most interesting comparisons is furnished by tests of the two cores, because they represent different materials, cross sections and flux-leakage paths. (In this report, "core" refers to the magnetic material around the coil and that in the armature.)

It is seen from Fig. 8A-R that the laminated core curve rises rapidly and evens off to a constant value (disregarding ripple), whereas the solid core starts more slowly but goes to a higher peak. This crossing is explained as follows.

At the beginning, the laminated structure responds quickly to current because first the laminations minimize eddy current losses that tend to delay establishment of magnetic lines across the air gap where they create the desired pull; and secondly, the "soft" iron in the laminations magnetizes readily. The solid iron, on the contrary, was slow-acting.

But the laminated magnet soon comes to its maximum force: for the cross section of iron is practically limited to the few laminations straddling the shaft (see Fig. 8*B*). The middle group of laminations on the armature and in the crosspiece of the core around the coil are interrupted by air gaps that practically keep the flux out of them until the other iron is saturated. Hence, iron-carried flux is low, much of it tends to loop around the coil rather than crossing the gaps to the armature, and electrode force (which depends on air-gap flux) is kept to a moderate peak.

After a slower start, the force in the solid iron magnet, having a higher air-gap flux, reaches greater peak values of electrode force. Even this, however, is a small fraction of the potential force calculated from the available ampere-turns and cross-sectional area of the core. Later designs show improvement in this regard.

Figure 8A-L with shunted coils shows similar differences. Rate of rise is lowered in all shunted tests. With 30% heat, the solid core takes current slowly, so that force at end of weld time is low but still rising. The laminated has reached its maximum force. If weld continued long enough, coil current would be the same for laminated and solid construction: the differences in force then would be due to area and (if unsaturated) to the difference in reluctance of the iron paths.

At 50% heat with shunted coils, these differences approximately counterbalance each other, so that solid and laminated reach about the same maximum force.

Figure 9 shows weld current polarity. When alternate polarity is used (i.e., each single-pulse welding current has an opposite polarity to the previous pulse), force rise is slowed because magnetic flux must be reversed before it appears as force on the electrode shaft. The continued negative polarity ("uninegative") gives faster initial force. Final force is about the same in either case if duration is sufficient. This difference does not appear when the laminated soft-iron core material is in the machine.

Figure 10 shows ripple. Low heats are obtained by using only a portion of the voltage wave. This produces ripples in the current wave, which appear also in the force wave with laminated cores. The presence of ripple in laminated, but not in solid cores, is explained by the eddy current and iron "softness" discussed under "magnet core."

Percentage of ripples (peak-to-trough, in relation to average force) for the conditions of Fig. 10 are:

Heat, %	*Ripple, %*
30	42.0
50	12.0
80	8.7

It is thought that the ripple may improve weld quality under certain conditions.

Derived Curves

Figure 11 shows peak force vs. heat setting. Higher heat (current) increases magnetic force until laminated core saturates at about 50%. With solid core, greater effective cross section prevents this, as discussed under Fig. 8.

Forces up to 3000 lb were produced by this magnet. There is no inherent limit to this value: Magnets can be designed for higher or lower force, according to the needs of the welding job.

Figure 12 shows peak force vs. air gap setting. Increasing the gap decreases the peak force, but not to the degree that might be expected. This is because (*a*) the gap cannot be made very small, as explained in discussion of Fig. 6; (*b*) this magnet is relatively inefficient (newer ones are showing improvement); and (*c*) air-gap adjustment is self-compensating to some extent because greater gap gives lower inductance, faster current rise, higher current yielding greater flux and hence higher force.

Figure 13 shows delay (*CD* on Fig. 3) in start of magnetic force build-up vs. heat setting. Considerable control was obtainable with shunted solid-core magnets, up to 2 cycles when heat was set at 30%. The laminated core did not respond; the force appeared instantly with current.

Figure 14 shows maximum rate of rise of magnetic force vs. heat setting. With the laminated iron, force rises instantly unless shunts divert the current. Shunts and air gap also govern the rate of rise with solid cores. Hence a desired value can be selected for the job in hand.

Supplementary Force-Time Curves

Figure 15 shows decay of magnetic electrode force. After weld current stops, the magnetic force is maintained (*EF* of Fig. 3) for approximately the duration of initial delay *CD*. It then decreases rapidly.

For those cases where control of decay is desirable to refine the weld, this can be attained by negative slope control of the current.

Figure 16 shows force-time curves with work in place. Insertion of aluminum produces but little difference in maximum welding force. The high resistance of the uncleaned aluminum of Curve 3 is responsible for the delay in force rise, and is one of those self-compensating effects that gives strong welds despite changes in welding conditions.

The relatively high resistance of stainless steel throughout its temperature range decreased the current and hence the welding force.

Satisfactory welds were obtained on all specimens with identical machine settings.

APPLICATION OF MACHINE CHARACTERISTICS TO WELDING

Before presenting examples of welds, we will outline the relations between machine settings and weld results.

The curves in the previous section show that a wide variety of force waves can be obtained on the magnetic force welder. To make the best use of them, we should know the corresponding requirements for each material and thickness to minimize expulsion, tip pickup, indentation and sheet separation, and to insure satisfactory welds.

Such information is, unhappily, not available in detail. We often know, however, what is required in a general way at each stage of the weld, and this is a useful guide for selecting the correct machine settings.

Initial Force

Low initial force may be utilized for several different purposes.

It aids the welding of high-conductivity materials... because low pressure means high electrical contact resistance that develops heat faster with a given initial welding current. Only the fast action of the magnetic force unit permits use of low initial force, followed by high tip pressures to retain molten nugget in place.

As a variation, weld surface burning can be prevented by use of low initial current, depending upon the high resistance to develop adequate heat.

Third, low initial force can prevent collapse of projections by being proportioned to embossment size and strength.

Magnetic Force Delay

It is often beneficial to maintain the low initial pressure for a cycle or two, retaining the increased heating effect of the high resistance until the workpiece begins to soften. High-conductivity materials may gain most from this effect.

Projections, on the other hand, tend to reach melting temperatures rapidly; hence short force delay is required.

Rate of Electrode Force Rise

This is probably the most important factor in making good welds on the rising force wave.

The ideal force pattern would be one that, at each instant during the heating process, maintained the work pressure between the minimum that will prevent expulsion and arcing, and the maximum beyond which there will be excessive indentation, sheet separation and tip pickup by diffusion.

The selection of such a force pattern should usually be possible by proper adjustment of the magnetic force machine; since, as seen from the curves of machine characteristics, the rate of rise, and even the shape of the force wave, can be controlled over a wide range.

There are certain applications in which maximum rate of rise is desirable. Projection welders, where there is appreciable setdown of the work, require adjustment of rise suitably to overcome inertia losses of the electrode head—which usually calls for maximum rate of rise such as obtained with laminated core.

High rates of force rise are also desirable where high heating rates and hence short weld times are used to localize the high temperature, as in spot welding near metal edges.

Effect of Line Voltage and Air Pressure Changes

Due to the compensating action of the magnet, it appears that the machine will be less sensitive to line voltage variation than standard welders; and, of course, air line pressure will only affect the initial electrode force which is a small fraction of the total, so will have little or no effect on the welds. This lack of sensitivity has been apparent in production.

Examples

Although the method of using the machine curves, Figs. 4–16, for selecting setups is still in the preliminary stage, some interesting cases illustrate the usefulness of this approach. Some of these examples are experimental; others are from production lines.

The machine characteristics were used in selecting adjustments for spot welding uncleaned 18-gage 2S and 3S aluminum sheet for a refrigerator part. A large number of commercial-quality welds were required with minimum tip pickup. On a standard machine, a maximum of only 28 spots could be made between tip dressings with the best techniques, including slope and taper controls on standard 60-cycle machines.

This work was brought to a Precision magnetic force welder. With a laminated core and no shunts—force rose too fast, as indicated at *A*, Fig. 17. The peak of the force wave had been reached before the nugget had formed, so the weld was, in effect, made under pressure that was both static and too high. The low resistance resulting from the high force decreased the heat so greatly that only a "good stick" was made.

Then, going to the other extreme, *B*, Fig. 17, many shunts were attached, decreasing rate and magnitude of force. Expulsion resulted.

By properly selecting shunts, welds were made on the rising force of sufficient magnitude (*C*, Fig. 17)—over 700 spots then being made before tip dressing became advisable. A continuous run of welds, each of which pulled nuggets of consistent size, is illustrated in Fig. 18. This shows the smooth surface and light indentation characteristic of aluminum welds with minimum tip pickup.

In passing, Fig. 17 shows in dotted lines the "ideal" temperature and force curves for welding aluminum alloys to military specifications. This desirable re-

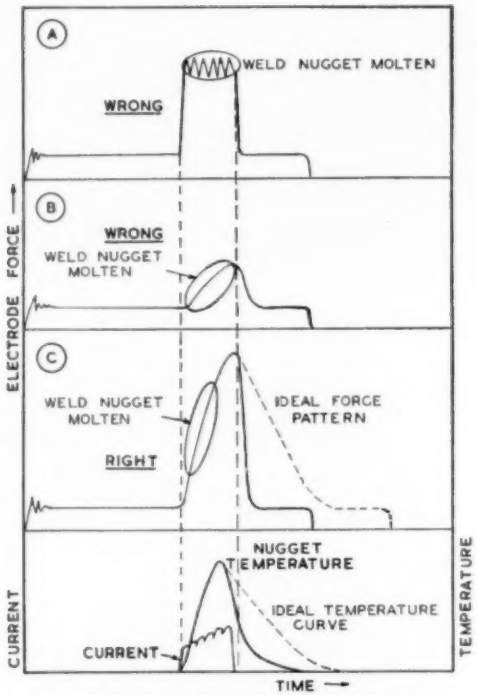


Fig. 17 Schematic arrangement of electrode force patterns in relation to the nugget formation

tention of force during a controlled cooling period was not included in the machine on which this work was done. It can readily be added for those few cases where it is required.

Unusual Welds

Proper selection of machine settings has permitted some unusual welds to be made—such as the cross-wire 3S aluminum $^{3}/_{16}$ in. diam shown in the middle foreground of Fig. 18, and the projection welds on 3S aluminum 0.090 to 0.064 in., uncleaned. In both cases the weld results were due to the unique ability of the magnetic force machine to bear lightly on the work before welding. Then with the welder set for a rapid rate of force rise under static conditions, it was able to overcome the inertia of the head and maintain the required minimum electrode force throughout the collapse of the projection. Welds were made one at a time.

The projections used on the 0.090-in. aluminum are shown in the left-hand triangular piece. The finished side of the 0.064-in. metal is shown on the round piece, about 3 in. diam. Next pair of parts show a set tested to destruction, with buttons still on the 0.090-in. piece and holes in the 0.064 in.

To indicate that this is not a critical setting, two thicknesses of 0.090 in. were welded; button pulled as at top left of Fig. 18.

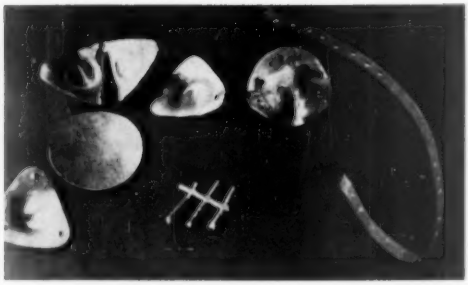


Fig. 18 Magnetic force welds on 3S aluminum

Right-hand ribbon: Spot welds, 0.050 in. *Center foreground:* $^{3}/_{16}$-in. wire. Four pieces alternating triangle and circles: Projection welds of 0.064–0.090 in. From left: 1—0.090-in. piece showing projections. 2—Welded assembly from finished side of 0.064-in. sheet. 3—0.090-in. piece showing buttons pulled. 4—0.064-in. piece showing holes pulled. *Pair of pieces at upper left:* 0.090-in. pieces welded at upper end with same machine setting as previous group. Large button pulled.

A closer view of the welded wire with the last weld sectioned (Fig. 19) shows a complete bond between the two wires. This can be accomplished with either more or less setdown than illustrated here.

Another application for which this machine is well adapted is projection-welding copper. Welding two 0.082-in. thick copper straps is shown at the left of Fig. 20. The projections are seen on the left strap; tested welds on the two middle specimens. Both unique features of the welder are used to make these welds—the low initial pressure increases contact resistance and develops more heat for a given current; the rapid application of force permits unusually high currents to be passed through the work, giving the heat concentration that makes the best welds on copper.

Fig. 19 Sectioned cross-wire weld from Fig. 18

Fig. 20 Copper-to-copper and tungsten-to-steel

Left-hand strap illustrates projections on 0.082-in. copper. Middle pieces show welds tested to destruction, pulling metal. At right, sintered tungsten contact disk, pulling hole in steel arm.

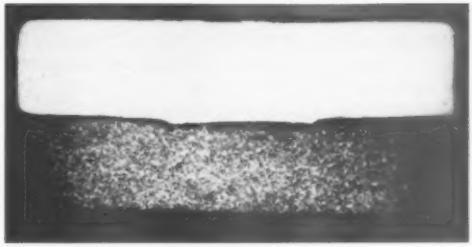


Fig. 21 Silver contact disk to brass arm

Strong welds, accurate dimensioning and long electrode life have resulted from production with magnetic force machine.

Tungsten-to-steel welds, the right-hand piece in Fig. 20, are another illustration of the versatility of this machine. Although it may be argued whether the joint is a true weld, it is strong enough to pull a button out of the steel sheet. Many thousands of contact points have been successfully welded without damage to the tungsten. This results from the high contact resistance obtained with light initial electrode force, followed by the driving action of the magnet in synchronism with the welding current.

Figure 21 is macrograph of a silver-to-brass contact from a production run on a 30-kva magnetic force bench welder. As with the previous illustration, the high contact resistance is utilized to permit low currents to prevent burning. The rapid application and accurate timing of tip force during the later part of the weld cycle is especially needed to prevent expulsion in this material that has a low plastic temperature range. The vestige of the coined projection in the silver may be seen in the photograph, but the difficulty in etching the silver makes it hard to determine the exact diameter of the weld. This product has an excellent service record.

For a final example, Fig. 22 shows top and side views of 20-gage mild steel sheets. Though the electrode overhung the edge, the weld is strong and there is only a small, smooth bulge of metal rather than the "whiskers" and ragged edge that might be expected. This improvement is attributed to the carefully controlled rate of force rise which permitted use of relatively high current and short weld time.

CONCLUSIONS

The force-time characteristics of the magnetic force welder can be accurately determined, in a form that shows the effect of each variable on the shape and magnitude of the electrode-force curve.

Such curves can be used to choose preliminary control settings and machine adjustments for making the first welds on new work; and to guide the changes to produce results by successive trials.

Certain advantages are noted when a synchronized current-force-time pattern is used instead of good average values of current and electrode force. Production experience has shown this to be particularly beneficial in welding high conductivity materials, and the tests indicate that it may be valuable in broader fields. Good welds have been made under conditions that create difficulty for conventional welders, for example: At edges of steel flange, silver to brass, tungsten to steel, copper to copper, projections on aluminum. Consistency of welds is improved by the compensating action of the magnetic force unit that makes these machines fairly insensitive to line-voltage changes normally encountered, and by the improved electrode life.

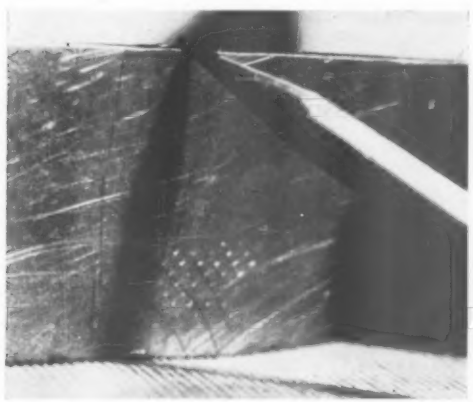
FUTURE DEVELOPMENT

By better design the magnetic circuit can be improved, increasing the force.

Improved methods of adjusting slope and magnitude of force are being developed, including some mechanical refinements and some new ideas in the magnetic circuit.

A 60-cycle a-c welder has been made and is being tested.

It is hoped that research in designing and applying this magnetic force principle will continue. The recording of machine settings for different applications will reveal the best relationship of the force pattern and current form for a variety of materials and applications. This would considerably ease the task of determining the best welding conditions on new jobs.



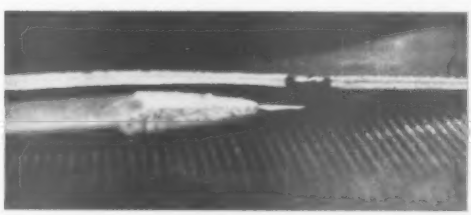


Fig. 22 Magnetic force spot weld on 20-gage 1010 steel, with centerline of tip at edge of sheet. Smooth condition noted on both top and side views

ACKNOWLEDGMENTS

Help from the following sources is greatly appreciated by the authors. Precision Welder and Flexopress Corp. for the use of the welder. The laboratory of a large industrial company, in which much of the work was done; the cooperation of the officials in providing facilities and of laboratory personnel in assisting with the work and providing helpful suggestions. Jerome Welch of Cutler-Hammer Corp. for use of Fig. 21, and his continued support in the development of improved welding with the magnetic force machine.

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Appendix 1

TEST CONDITIONS AND OPERATING PRECAUTIONS

The welder shown in Fig. 1 was built for the comparison of a standard mechanical system with the magnetic force device. Ordinarily three-phase welding with air pressure was carried out using the standard lower horn, not shown in the picture. Magnetic force tests were made by inserting the other horn, shimming the magnetic air gap to the desired value, loosening the upper electrode holder, bottoming the ram, bringing the upper electrode into contact with the lower one or with the work and then tightening the upper holder. Full air pressure was applied to the upper cylinder to furnish a solid base against which the magnetic force acted.

Most tests were made with 6-in. radius No. 2MT electrodes butting against each other. . . as near to RWMA conditions for short-circuit tests as the present machine permitted.

The range of air gaps was determined by machine design. The minimum was the gap which would not close during the force cycle, as the horns separated slightly under the load.

Until the electrodes wedged tightly into their holders, the air gap had to be watched carefully since hammering in the tapered tips shortened the air gap.

Instrumentation Precautions

Amplifier strain gage was balanced at frequent intervals by self-contained adjustments.

Good water cooling of electrodes was required to prevent thermal expansion from giving faulty strain indication—not to mention prevention of undue tip pickup when welding aluminum.

At the start and finish of each day's run, the oscillograph force deflection was calibrated against a static pressure gage, the horizontal component against a 60-cycle time wave.

There was sufficient time between welds to permit the magnetism to discharge from the electromagnet through a discharge resistor that was connected across the coil.

WELD TECHNIQUE FOR FIGURES 18–22

Figure→	18				20	21	22
Detail→	*Ribbon*	*Wire*	*Sheet*	*Triangle*			
Material→					*Copper*	*Silver*	*Steel*
Upper	2S		Aluminum		Commercially pure	Contact	1010
Thickness, diam, in.	0.050	3/16	0.090	0.090	0.002	0.040	0.037
Proj. diam	None	...	0.175	0.175	0.150	0.060	None
Lower	2S		Aluminum		Copper	Brass	1010
Thickness, diam, in.	0.050	3/16	0.064	0.090	0.082	0.035	0.037
Surfaces	Brushed	Brushed	Not Cleaned		Brushed	Not	Not
Welder on which work was done, kva*	30	100	100		100	30	100
			Machine settings and adjustments				
Air force, lb		280	280		300		350
Magnet core		Lam.	Lam.		Lam.		Lam.
Air gap, in.		0.125	0.25		0.25		0.25
Shunt, in.		9/16	9/16		1/16		1/16
Heat, %		58	59		100		60
Cycle		Full	Full		Full		Neg.
Weld duration in 60-cycle timing, cycles		4	6		30		4
Electrodes—RWMA Class 2 throughout							

* The 30-kva machine is described in Reference 1; the 100-kva machine in this paper.

Appendix 2

INSTRUMENTATION

Pickup

Force. Pair of strain gages, Baldwin Locomotive Type SR4, 1/4 in. long, gage factor 1.8. One cemented longitudinally to lower electrode; other hanging free for reference. Gages and leads shielded with braided aluminum.

Dyna-Mike strain-gage pickup and amplifier with 8000-cycle carrier wave and band-pass filter. Industrial Electronics Model 1290, with Model 166 bridge. This was the only method that recorded force without excessive inductive pickup which was particularly bad because of the stray flux from the electromagnet in the welder.

Current. RPI manganin shunt built into electrode holder. Toroid around upper tip.

Recorder

DuMont Model 304 5-in. oscilloscope, with attached Land camera Type 297.

Direct Measurements

Force. GE 2000-lb force gage for static calibration.

Resistance. Pogo Type PB cable tester, for welder secondary.

ALUMINUM CLEANING

The test strips were cleaned by Diversey Corp. process, *Bulletin 49D*, with 5 min degrease in No. 36 solution, 6–10 min wash, 6 min etch in No. 514, 5 min wash.

Surface resistance measurements on material so treated were consistently 6–10 microhms.

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(*Continued on page 1199*)

Large-Scale Welding Done on Chevrolet Front Frame Members

More than four million were completed in less than three years. Each assembly includes two "three-o'clock" submerged arc welds, several hand arc welds, two butt and several spot and projection welds

by L. E. Feeney

IN SOME respects, the front frame member of Chevrolet passenger cars is among the most important, unusual and interesting assemblies of stampings to be found anywhere. Its largest component is a trough-like $\frac{1}{8}$-in. thick steel stamping having a U-shape section throughout most of its length. Outturned flanges at front and rear are joined by numerous spot weld to a mating plate that closes the U.

At each end of this assembly there is a stamped well or spring housing of steel 0.100 in. in thickness. Each of these housings is drawn in two halves that are joined by a butt weld. Later each spring housing is provided with a separately stamped spring seat fastened in place by projection welds.

Subsequently, the two spring housings are joined to the ends of the major crossmember by two three-o'clock submerged arc welds that follow an arc of 180 deg. Both of these welds are made simultaneously in special machines developed and built for the purpose by Chevrolet.

There are, besides the components just mentioned, several other minor ones in the assembly and they are applied by various welding methods. The completed assembly represents an unusual achievement in welding. From December 1948 to April 1952, 4,232,951 of these weldments were produced. They involved the use of 160,993 lb of welding wire and 651,911 lb of granular welding flux.

Each of the final assemblies is subsequently built into the frame for a Chevrolet car and during its life is subjected not only to the weaving and torsional stresses that frame members receive, but to the constant and often severe stress imposed by a "knee-action" front suspension system. It is a tribute to good engineering and production practice that a structure of this type, though comparatively light in weight, meets all service requirements over periods of many years. Frame front members are built complete in the Chevrolet Gear and Axle Divisions' plant, Detroit.

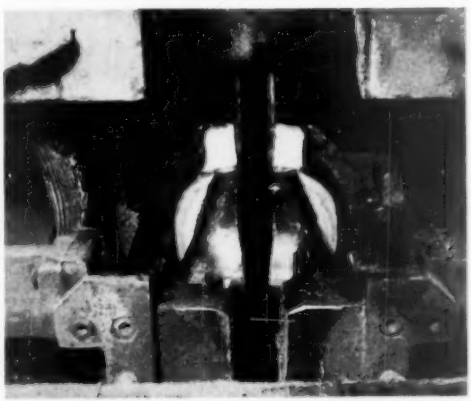


Fig. 1 Two halves of a stamped spring housing for a Chevrolet front frame crossmember are here shown in the dies of a butt welder ready to make the weld as the dies are closed

Fig. 2 Completing the butt weld that joins the two halves of a spring housing and makes it ready for some secondary operations before the housing is arc welded to mating components

L. E. Feeney is Plant Engineer of the Gear and Axle Plant, Chevrolet Motor Division, General Motors Corp., Detroit, Mich.

Joining of the two halves of spring housings is performed by machine butt welding using setups like that shown in Fig. 1. The two halves are loaded by hand and are air clamped in the dies which are then closed to produce the weld. This weld is in a vertical plane, as shown in Fig. 2. Ten of these machines are used.

Subsequently, each spring housing receives a spring seat which is projection welded in setups like that in Fig. 3, with the housing air clamped in the lower die and the seat attached to the upper die which is lowered to effect the welds.

Major joints required to fasten spring housings to the U-section crossmember are welded with the submerged arc process, welds being made in the three-o'clock position as shown in Figs. 4 and 5. These make use of machines designed and built by Chevrolet and they are believed to be the first machines to do automatic arc welding of the three-o'clock type on a high production basis. Five of these machines are employed and each makes a pair of arc welds per piece.

Each machine has two automatic Lincoln welding heads, both equipped with a fillet welding wire guide attachment. Each head is served by a welding d-c generator of the same make. Welds are produced at 27 v and 370 amp, each weld being 6 $^{7}/_{8}$ in. long. As the welds are horizontal and each joins surfaces that are nearly vertical, the electrode wires which are $^{5}/_{32}$ in. diam are fed through guides so arranged that the wire at the arc is only slightly inclined above the horizontal. Although the welds are classed as of the three-o'clock type, the tip of each electrode is actually more nearly in a two-o'clock position.

Two men work at each machine. One loads the crossmember and two spring housings and clamps them in a fixture. He also places a tray just below each joint to be welded and uses a scoop to fill the trays with granular Lincoln flux to such a level that it will cover the arc as the weld is produced. Trays have an arc shape that conforms to the shape of the weld and keeps the flux where it is needed. Flux is not fed continuously as in most other submerged arc welding.

While the fixture at one side of the machine is being loaded, that previously loaded is in welding position where the operator starts the automatic cycle and sees

Fig. 3 Placing a previously butt-welded spring housing in the lower dies of a machine that holds the housing while a stamped spring seat, held by the upper die, is applied by projection welding

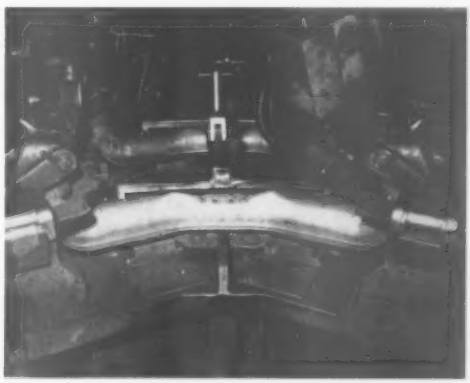


Fig. 4 Setup in the machine that automatically arc welds the two spring housings to the ends of the main crossmember. Two Lincoln heads feed the welding wire automatically as they are swung slowly through an arc of 180 deg

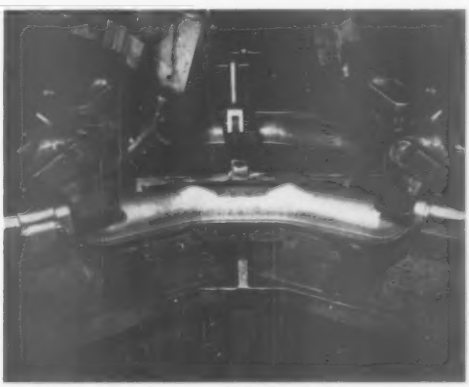


Fig. 5 Same setup as in Fig. 4 but with the heads in a different angular position. They make three-o'clock arc welds. While welding, the tip of the electrode and weld head are covered by granular flux (some of which has been removed in this view to show parts of the weld heads) placed in the trays set just below the welds

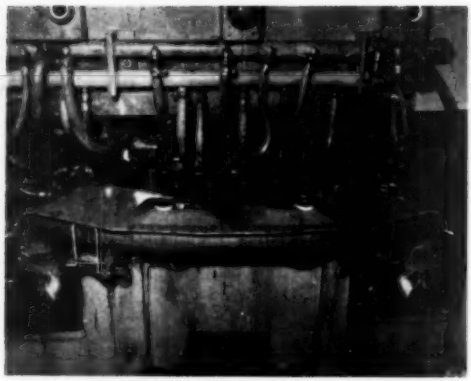


Fig. 6 Completed front crossmember (in inverted position) as it appears after removal from the spot welding machine in the background. This welder joins the cover plate to the U-shape channel member

that the welds are properly made. When he presses the start button, each welding head moves down and then outward automatically so that the welding wires contact the work and strike the arcs. Immediately the arcs are struck, the heads are given a rotary motion that causes the arcs to follow the joints and produce the welds. After each head travels 180 deg it retracts and breaks the arc as the welds are completed.

When welds in one setup are completed, the heads move to the second setup and repeat a similar cycle. While it is in process the operator at the side where welds have been completed removes the trays of flux and sumps them on a screen to separate out any fluxed lumps. He then unlocks and removes the weldment, puts a new set of components in place in the fixture, locks them, replaces the trays for the flux and refills them with screened flux, so that all is in readiness for the next cycle.

In this way the operators load and weld alternately and the two welding heads are kept in almost constant use, whereas in most automatic arc welders, the heads remain idle during loading and unloading periods. Flux used at each machine is kept in a small bin beside each machine and that sifting through the screen above is reused.

Arc welds made in the automatic machines are of uniform high quality and are produced at a rapid rate. They do not complete the attachment of spring housings, to the assembly, however, because the remaining contour of the housings is irregular in shape and not adaptable to automatic welding. Consequently, after removal from the automatic machines, the weldments are placed on a chain conveyor that carries them through booths in which hand arc welding is done.

In each of these booths, one man using conventional hand equipment works at each side of the conveyor and does welding only on the end of the weldment closest to him. Two plug welds are made at each end in the first booth and two short side arc welds in the second booth. Weldments are advanced continuously on this line, but chain speed is so adjusted that skilled operators have no difficulty in keeping pace with the work.

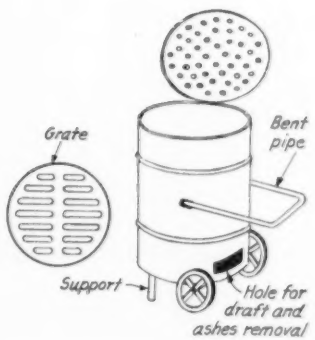
For the final welding operation on the crossmember, the weldments that have passed through the operations already described are set into automatic spot welders of the type shown in Fig. 6 along with preformed cover plates that close the opening across the U-shape portion of the assemblies. These machines have a series of electrodes, some vertical and others set at various angles. When the components to be joined are in place and clamped, the machine makes the spot welds automatically in the required sequence but so rapidly that they seem to be made all at one time.

This completes the welding of the crossmember and it is ready for inspection and advance to other departments in which suspension and other components are applied.

Portable Incinerator

NO ONE likes to have an unsightly incinerator in his back yard as a permanent fixture. Here is a portable one that you can wheel out of sight when not in use. To make it, first *thoroughly clean and purge an old oil drum.** Then weld an axle to the bottom for a pair of wheels.

Weld a suitable grate about 6 in. above the bottom of the drum, and cut an opening—an inch or two below the grate—for draft and removal of ashes. To keep hot ashes from blowing, it is a good idea to add a cover made from wire mesh or perforated sheet metal.



* See American Welding Society booklet A6.0-52, "Safe Practices for Welding and Cutting Containers That Have Held Combustibles."

the AMERICAN WELDING SOCIETY

activities • related events

Houston Welding Convention and Exposition

Tuesday June 16, 1952—Friday, June, 19, 1952

For more than a decade it has been the hope and ambition of many leaders of the Welding Industry and the AMERICAN WELDING SOCIETY to hold a Convention and Exposition devoted exclusively to welding and closely allied processes. The need to include other geographical centers than those so ably served by the Metal Congress and Exposition has become increasingly apparent. One of these areas is undoubtedly the great Southwest rapidly growing in stature to that of an Industrial Giant.

Tentative feelers put out as to the best location found Houston, Tex., in the forefront as one of the most suitable locations to launch this first venture.

The diversified industries in this area—chemicals, oil, structural, pipe lines, marine construction, aircraft, hardfacing and many others—furnish the necessary background and foundation for the success of an exposition. The famed Shamrock Hotel provides adequate housing, convention and exposition facilities in comfortable and pleasant surroundings.

Texas with its usual hospitality and knack of getting things done quickly indicated that it is ready, able and willing to do its part to make this first All-Welding Convention and Exposition a huge success. The rest is up to the membership of the SOCIETY and to the manufacturers.

The Exposition is rolling along at an unprecedented pace. More than half the Exposition space has been spoken for by more than thirty exhibitors (November 1st)—others have indicated a definite interest.

The National Program and Papers Committees have already canvassed the needs of this area and present herewith in broad strokes the general outline of the technical sessions. Details will be filled in as rapidly as possible.

Members desiring to present papers at the Houston Meeting should send in their suggestions to the National Papers Committee. Practical papers and those beamed to fit in the needs and interests of the Southwest will be favored. **All suggestions must be received by January 15th.**

Tentative Outline of Technical Sessions on Which Papers Are Desired

Marine Structures: The welding and maintenance of drilling platforms and the welding of marine oil well casings and other marine structures peculiar to this area will be covered.

Resistance Welding Symposium (including aircraft): A symposium-style session with carefully planned short practical papers, including spot and projection welding, will be arranged under the auspices of the Technical Activities Committee.

Resistance Welding Symposium on Seam and Flash Welding will be arranged under the auspices of the Technical Activities Committee.

Welding High Strength Pipe Lines: Papers desired.

Structural Welding: One of the most progressive areas in the United States in so far as the application of structural welding is concerned is the State of Texas.

Welding of Titanium: The Army Ordnance will present additional research and applied papers in this field.

Hard Facing and Metallizing: Papers in these fields are particularly requested by this region and suggestions for interesting papers will be welcomed.

Inspection Methods: A session on nondestructive testing was suggested by the Houston district and suggestions for papers in this field will be welcome.

Welding Grade Steel: A technical session, forum or symposium on the characteristics of steel for welding was considered to be one of the most worth-while subjects. It was suggested that short papers on pipe-line steel, structural steel, pressure vessel steel, and the effect of material characteristics when welding under various climatic conditions should be scheduled. This would be followed in the afternoon by an open discussion from the floor on the whole subject. These sessions will be under the auspices of the Technical Activities Committee.

Human Element in Welding: Papers in this field are desired.

Welding of Chemical Equipment for Low- and High-Temperature Service: Suggestion was made that papers on the welding of aluminum, stainless and chrome

We sincerely appreciate the valued support, assistance and guidance of all AWS members and the welding and allied industry throughout the year, and extend our hearty wishes that you may enjoy a very

merry christmas

and a

happy new year

AWS NATIONAL AND SECTION OFFICERS AND AWS NATIONAL HEADQUARTERS STAFF

moly would be well received in the Southwest.

Inert Gas Welding: The Southwest desires a whole session be devoted to this subject and that at least one paper on welding mild steel should be included. Suggestions for papers are desired.

Miscellaneous: Papers are desired on furnace and induction brazing, welding of tankers for maintenance against corrosion, fabrication of pipe including resistance welding, and the effect of preheating and postheating when welding structures.

Other Features

Plant Tours: A number of plant tours will be arranged.

Entertainment Tour: Tour of Ship Channel—possible boat trip to San Jacinto Inn for ladies and bus tour of Ship Channel industries for men, with cocktail hour and Gulf seafood and chicken dinner at San Jacinto Inn as climax.

Guest-Room Facilities: All guest rooms and suites are "outside" rooms, with tub and shower, are individually decorated and contain custom-built furnishings. All are equipped with wall-panel button radio control and Muzak, and with individual air-conditioning controls. One-room suites, called Doublettes, and parlor and bedroom suites, carry the same rate for one or two persons. Forms for reservations will be mailed January 15th and assignments made by the Housing Committee by March 15th.

Restaurants: In addition to the evening supper clubs (Emerald and Shamrock Rooms), there are the Pine Grill and Aquatic Terrace for all meal service—breakfast, lunch and dinner. Breakfast from $1.00 up; luncheon $1.75 up; dinner $2.75 up.

Recreation and Entertainment: Hotel provides outdoor swimming pool, aquatic terrace, tennis courts, putting green, music department of Dixieland Band, Hillbilly Orchestra, Strolling Guitar Player, Shamburger picnic provision, specialty stores.

To Exposition: Lobby of Hotel is at ground level, same as Hall of Exhibits, connected by covered (enclosed) walkway to Hall. One-half minute from Hotel Lobby to Exposition front entrance.

Overflow: Houston has 3435 air-conditioned guest rooms in its many first-class hotels and 286 in its deluxe motels. For those not desiring guest accommodations at the Shamrock, and for overflow beyond Shamrock capacity, arrangements will be made through the Houston Chamber of Commerce, or direct, for placement in order of preference and available accommodations.

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Fourth Meeting Board of Directors

The Fourth Meeting of the year of the Board of Directors was held in Room 127, Hotel Statler, New York, N. Y., on Tuesday, Aug. 26, 1952, at 10 A.M., with the following in attendance:

Members: Chairman C. H. Jennings, F. L. Plummer, R. S. Donald, L. C. Bibber, T. J. Crawford, A. F. Davis, LaMotte Grover, H. L. Ingram, Jr., I. A. Oehler, H. E. Rockefeller and J. R. Stitt.

Staff: Secretary J. G. Magrath and Assistant Secretary F. J. Mooney.

Guests: G. O. Hoglund, Chairman, National Program Committee; G. Van Alstyne, Member, Special Committee on Annual Meeting; J. Landau, Auditor.

Nominating Committee Procedure

The recommendation of O. B. J. Fraser at the Executive Committee meeting on June 12, 1952, that the Board of Directors consider the present By-Laws in regard to membership on the National Nominating Committee was now discussed. These By-Laws are so written that they do not exclude the possibility of a nominated officer or a member of the Board from serving on the National Nominating Committee concurrently. Mr. Fraser recommended that provision be made for the retirement of the officer from one or the other office, preferably his office on the Nominating Committee. The attending members believed that the best procedure would be that Mr. Fraser's recommendation be tendered to the By-Laws Committee as a suggestion for their consideration in their review of the total revision of the Society's By-Laws, an assignment given to them by the Executive Committee at an earlier meeting.

API-AGA Joint Committee Action

The TAC withdrew membership from

the new Koolite* torch...

we sold, to date, 102,396 of this new model torch... and of a prior model over 150,000

it uses either oxygen or compressed air with any of the useable fuel gases—city and natural gas, propane and butane or acetylene.

just select the proper tip type and sizes

it gives you a needle point or brush like flame

This new Koolite Torch offers many advantages — it is always cool to the touch because of its smooth, long lasting plastic handle. The blowpipe is designed to be used with either compressed air or oxygen and any of the useable fuel gases. It will produce a brush like or a needle point flame. The three tips are easily exchangeable. Clear operating instructions are on the box as is also an understandable replacement parts list. You will like the reasonable price and the fine performance of this torch. It is made by one of the oldest welding equipment companies.

*The word "Koolite" is a registered trade-mark.

Price - - - with three tips and wrench only $9.50

Made by National SINCE 1910 WELDING EQUIPMENT CO., San Francisco 5, California

821

an API-AGA Joint Committee on Oil and Gas Pipe Line Field Welding Practices because that Committee, despite the protests of two of its members appointed by AWS, approved a standard which, in the opinion of AWS representation, was inadequate and possibly harmful. The Secretary of TAC accordingly had officially, for TAC, so advised the Chairman of the foregoing Committee. A member of the SOCIETY's Executive Committee questioned if it was not advisable that the Board of Directors send a letter to the API supporting the action of TAC and strengthening the impression that the AWS was in protest to the API-AGA joint decision. After discussion in regard to whom such a letter should be sent, it was agreed by all that the best procedure would be for the SOCIETY's Secretary, acting for the Board of Directors, to send a letter to R. G. Strong, Chairman of the API-AGA Joint Committee on Oil and Gas Pipe Line Field Welding Practices, confirming and approving the action taken by TAC and sending a copy of that communication to the Secretary of API. The Secretary was advised to consult with TAC on the preparation of context of letter.

Action: Upon motion, duly seconded, the Board of Directors ratified action taken by TAC and approved the foregoing procedure for the Secretary.

National Officer Selection

A. F. Davis recorded a complaint that he received from at least four different people in the Cleveland area to the effect that National Officers of the SOCIETY were not being picked from users of welding. The complainees felt that officers of the SOCIETY should not be chosen from companies having a commercial interest in selling the welding processes. Mr. Davis, member of the Board of Directors, stated that he concurred in the feeling of the complainees. The SOCIETY's Secretary observed that in the order of our By-Laws, all members of the SOCIETY excepting the C and D classifications (Associate and Student Members) are eligible for national office regardless of occupation and that such a move would require a change in the By-Laws which, in turn, would require submission to the membership for vote. Discussion followed and generally it was the feeling of the balance of the members attending that the individual's occupation should have no bearing whatsoever upon his eligibility for national officership. It was also observed that in the course of the SOCIETY's Presidential Chair history, there were not more than five or six presidents of the SOCIETY who had been connected with companies who sold welding products. In the greater majority, the Presidential Chair has been occupied by representatives from user industry. No action was taken on this item.

1953 Spring Meeting

The Secretary reported on activities to date since the last Board of Directors' and Executive Committee meetings. He advised that he had engaged the facilities of the Shamrock Hotel and Shamrock Hall of Exhibits in Houston, Tex., for the purpose of the SOCIETY's holding a National Spring Meeting and a National Welding and Allied Industry Exposition on a modest scale, on the dates June 16–19, 1953. He received approval from the Fire Department to permit the use of oxy-acetylene and electric welding demonstrations in the Hall of Exhibits. He recommended providing the Secretary the assistance of an action committee with personnel located in New York, which would allow him to proceed under their direction with further preparation activities and the production of putting on an Exposition. The Secretary advised that industry in September would begin preparation of budgets for the forthcoming year and it was essential that industry immediately receive notification of this forthcoming event so that they could prepare for such in their budgets for 1953. Accordingly, he has prepared a rough draft of a circular which is to be sent to all industry. He submitted copies of draft to the attending members of the Board for their review. The Secretary also submitted rough figures of estimated attendance, expense and income, and requested that the SOCIETY underwrite this activity for $5000, such monies to be provided from the surplus of income over expense during the 1951–52 fiscal year, as indicated in the report of the Finance Committee based upon 10-month operation. He asked that the action committee be given full power to make any required decisions even to the extent that should their findings indicate that it would be advisable to again consider the farming out of the management of this activity to an agency, they would have the power to do so. In the discussion following, it was agreed by all attending members that the time had come for the SOCIETY to take this step which would serve the welding industry in areas not heretofore served by the National Metal Exposition. It was the general feeling of the attending members that in no way did the proposed activity at Houston interfere with the Western Metal Congress and Exposition which will be held in March 1953 in Los Angeles, with the National Metal Congress and Exposition which will be held in October 1953 in Cleveland, and that the decision by Mr. Eisenman to hold a Southern Metal Congress and Exposition in Houston in March 1954 likewise did not affect our proposed plans for an All-Welding and Allied

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with . . .
MANGANAL
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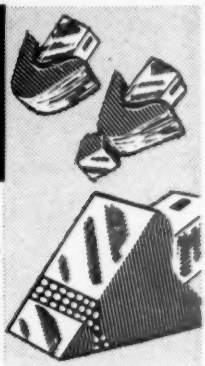
11% — 13½% MANGANESE - NICKEL STEEL
WEDGE BARS



LASTING RESULTS — Manganal Wedge Bars are made of the toughest metal known . . . work-hardens to 550 Brinell . . . minimizes impact and abrasive wear . . . outlast new teeth.

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INEXPENSIVE - Longer-lasting results combined with speed of application make Manganal Wedge Bars your best buy.

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Literature on latest methods for speedy and economical repair of worn equipment.
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SOLE PRODUCERS 92 N. J. RAILROAD AVE. NEWARK, N. J.

NEW LINCOLN PLANT CREATED BY INCENTIVE-INSPIRED CO-ACTION IN DEVELOPING POSSIBILITIES IN PRODUCT

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WELDED DESIGN PUTS RIGHT MATERIALS IN RIGHT PLACE TO SIMPLIFY PRODUCTION...CUT COSTS 57%

By **Hugo C. Klix,** Planning & Methods Supervisor
The Lipe-Rollway Corporation,
Syracuse, New York

IN redesigning the bar feed legs for our product, welded design provides a stronger, more durable construction yet one that can be produced from less material and at a 57% saving in cost.

The original design in gray iron (Fig. 1) entailed costly operations of machining and assembly. With our present welded steel design, the shop has simply to saw steel components to length from standard mill shapes. These components are then held in plain clamp-type fixtures and fillet welded with fast, downhand welding techniques.

The welded design also gives the shop greater flexibility of operation. Since patterns are no longer required, former maintenance and storage costs are eliminated.

Fig. 3. Bar Feed *of welded design manufactured by Lipe-Rollway Corporation, Syracuse, New York. Welded steel gives added strength and rigidity, has neater appearance.*

PROPER DESIGN IN WELDED STEEL IMPROVES PRODUCT LOWERS COST

Fig. 1. Machine Stand *originally made from gray iron used steel tube for column. Top and bottom members required milling and drilling. Weight, 54 pounds. Cost, $22.58.*

CUTS COSTS 57%

Fig. 2. Present Welded Steel Design *for maximum economy uses standard steel shapes wherever possible. Weight . . . only 47 pounds. Cost . . . $9.71 . . . a saving of $12.87 per unit.*

STRONGER, MORE RIGID PRODUCT

Machine Design Sheets available on request. Designers and Engineers write on your letterhead to Dept. 912.

THE LINCOLN ELECTRIC COMPANY

Cleveland 17, Ohio

THE WORLD'S LARGEST MANUFACTURER OF ARC WELDING EQUIPMENT

Industry Exposition in Houston in June 1953. Discussion was held in regard to the rental fees to be charged for space and it was finally agreed that the rental fee as provided on the Secretary's proposed first publicity release, which was in turn recommended by one of the potential large exhibitor's Advertising Director, was satisfactory. It was recommended by the attending members that a terminal date be established for return of advance reservations and that the initial publicity should carry that terminal date. In further discussion, Oct. 15, 1952, was established as the terminal date for *advance* reservations. On the basis of suggestion by the Chairman, actions were taken in the order following

Action: (*a*) Upon motion, duly seconded, it was voted that the SOCIETY should conduct and manage its own Exposition without the assistance of an outside agency.

Action: (*b*) Upon motion, duly seconded, it was voted that the Secretary immediately proceed to send out the first publicity release to the welding and allied industry.

Action: (*c*) Upon motion, duly seconded, it was voted that the SOCIETY underwrite this activity to the extent of $5000, such monies to be taken from the 1951-52 surplus, if necessary.

Action: (*d*) Upon motion, duly seconded, it was voted that an "action" committee be appointed by the Chair.

Budget Approval

Proposed budget of income and expense, as previously submitted to the Board of Directors, showed a net expense over income of $4883.50. On the basis of the SOCIETY conducting a Welding and Allied Industry Exposition in the spring of 1953, it was felt that the income to be derived from this activity should exceed expenses incurred and on this basis the Board of Directors instructed the SOCIETY's auditor to provide for income and expense items resulting in a balanced budget for the 1952–53 year.

Action: Upon motion, duly seconded, budget of income and expense for the 1952–53 fiscal year was approved in the order of the auditor's balancing said budget as so recommended.

East Texas Section

The Assistant Secretary advised that a group centered about Longview, Tex., with a nucleus at LeTourneau Institute and various East Texas oil industry organizations, has accomplished the enrollment of 50 AWS members and formally requests authority to operate as the East Texas Section of the SOCIETY beginning Sept. 1, 1952, having made all necessary steps for the establishment of a Section.

Action: Upon motion, duly seconded, the Board of Directors so approved.

By-Law Amendments

The Secretary advised that the letter ballot on the last proposed AWS By-Law amendments, as issued on Apr. 25, 1952, and as a result of previous action taken by the Board of Directors, resulted in a return of 1781 ballots. This total consisted of 1666 blanket approvals; 14 blanket approvals with comments; 64 disapprovals in part or in whole; 37 without signature or failed to check either approval of disapproval which were therefore disqualified. The By-Laws provide that amendments shall be adopted at the expiration of 60 days after mailing ballots, unless rejected by at least 20% of the eligible voting membership. The foregoing returns totaled (after deduction of disqualified ballots) 1744. For rejection there would have been required 349 disapproving ballots. Accordingly, on the basis of the By-Laws, the amendments are approved by the membership. The Secretary observed that several ballots were returned indicating disapproval of sending out letter ballots to the membership for this type of decision. It was expressed by the members that this was a waste of the SOCIETY's money and the members' time and that some of these decisions should be left to the Board of Directors. At the previous meeting of the Executive Committee and that Committee voted to ask the By-Laws Committee to review our By-Laws on the basis of providing rules of procedure which would allow changes to be made by the Board of Directors.

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A Division of Air Reduction Co., Inc.

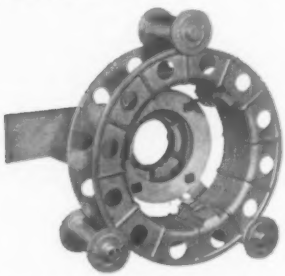
NOW

Double and Triple weight coils for automatic welding with the new REID-AVERY (free wheeling) REELS for Submerged Arc, Open Arc, and Gas Shielded Arc Welding

Raco Reel for 150-200# coils with floor mounting stand.

Decrease down-time by using larger coils. Machines now equipped for 25 lb. coils can use up to 75 lb. coils with these new reels.

These cast aluminum alloy reels ride on ball-bearing trunnions with deep flanges. The wire uncoils with remarkable ease and yet there is no chance of overrunning and throwing loops over the side. No brakes or drags of any kind are necessary. The wire itself prevents overrun. These "free wheeling" reels provide the absolute minimum load on the automatic welding machine.



Raco Reel for 25#-50#-75# coils. Mounted on popular submerged arc machine.

The Reid-Avery reels are available with stands for floor mounting or with special spiders for direct attachment to most popular automatic welding machines. Three reels 12" ID x 4" wide, 14" ID x 6" wide, and 25" ID x 4" wide nominal size are in stock for prompt delivery.

Raco Reel shown split for loading.

Note the simple split reel construction with suitcase type latches. Coils can be replaced in seconds and no wrenches or special tools are required.

We are equipped to supply all sizes of layer wound coils for these reels, for other reels, or on expendable wooden spools where required.

The REID-AVERY COMPANY

INCORPORATED

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SINCE 1919 PRODUCERS OF ARC WELDING ELECTRODES AND WELDING RODS

NEWS OF THE INDUSTRY

Machine Gas Cutting Film

"Burning Blades," a new three-reel Kodachrome sound motion picture, just completed by Air Reduction, was shown for the first time at the National Metal Exposition.

This film covers in detail the application of machine gas cutting to modern industry. It shows how this equipment can accurately cut intricate shapes in steel plate or slice through a 5-ft chunk of steel scrap. Among the machines shown in operation is Airco's new No. 50 Travograph.

The oxy-acetylene torch is shown at work in shipyards; in manufacturing plants cutting electronic parts, structural pieces and machinery components; and in steel mills reducing large pieces of scrap to the proper size for use in making steel. Showing time: 29 min.

Piping Code

A safety code for gas pipe lines and distribution systems—covering material, design, fabrication, installation, testing and operation—has just been approved by the American Standards Assn., 70 E. 45th St., New York 17, N. Y. This document is the new Section 8 of the American Standard, Code for Pressure Piping, B31.1-1951. This new section, designated as B31.1c-1952, is a consolidation of those parts of the existing Code for Pressure Piping pertaining to gas piping.

Section 8 of the Code for Pressure Piping was prepared and published separately as a guide in the construction of pipe lines for gas transmission and distribution to meet a current need in connection with the construction of cross-country gas pipe lines. The separate publication eliminates the necessity for cross-referencing to other sections of the code. The new Section 8, whose technical requirements are identical with those in the code, will be included as the last section in future editions of the Pressure Piping Code.

Chairman of the subcommittee which consolidated the new section is Frederic A. Hough, Vice-President, Southern Counties Gas Company of California, Los Angeles. Subcommittee No. 8 has a membership of 52 representatives of gas transmission and gas distribution companies; pipe companies; valve and fitting companies; independent research groups; technical universities; consulting engineers; and government agencies.

The new standard has chapters dealing with piping components, pipe joints, fabrication details, and requirements after installation, and the final chapter consists of tables and illustrations.

In developing Section 8 for separate publication, the subcommittee reviewed specifications and codes already formulated or in the process of preparation by other agencies, and drew upon the resources of the technical groups represented in its membership. The major consideration of the group was the public safety aspect of the specifications.

Sponsoring organization for the Section 8 code is the American Society of Mechanical Engineers.

Awards Made for Welded Bridge Designs

The James F. Lincoln Arc Welding Foundation of Cleveland, Ohio, has announced awards in its 1952 bridge design competition, Welded Bridges for Steel Conservation. A. F. Davis, Foundation Secretary, stated that the Award Program, sponsored to encourage the creation of bridge designs which conserve steel, attracted designs from the foremost bridge designers in all sections of the country. Designs showed an average saving in steel, made possible by welded construction, of approximately 20%.

First Award of $7627 went to Elwyn H King, Long Beach, Calif., whose welded design of a 7-span deck girder bridge indicated a saving in steel of 24% over what was actually used in erecting the bridge by riveting.

Kiser Dumbauld, of the Ohio State Highway Department, received Second Award of $3635 for his redesign of one of the State's bridges. His welded design indicated that a saving in steel of 18% would have been made if the bridge had been welded. Dumbauld has received main awards in two previous design programs sponsored by the Foundation.

Third Award of $2078 was shared by Thomas Kavanagh, of New York University and Leo Coff, New York Consulting Engineer. Kavanagh has recently been made Head of the Department of Civil Engineering at New York University, and this award is the third time he has been similarly honored by the Foundation in bridge design programs. The welded design created by Kavanagh and Coff showed a steel savings of 21%. Ten Honorable Mention Awards were also made.

Dumbauld, Second Award winner, in a discussion of his design, stated that if welding were used in bridges in place of riveting, approximately 73,000 tons of steel would be saved every year in the bridge work on all public roads, streets and highways. He also said that in 1952, welding would have saved 40,000 tons of the NPA allotment of steel to the Bureau of Roads. His estimates were based on government figures of planned and in process bridge work.

The Foundation has published two books presenting the significant results of the work its award programs have stimulated and is planning a third based on the competition just closed. Several bridges based on award designs have already been or are being erected, notably in the State of Washington and in Mexico. More states appear to be adopting welding because of its weight and cost saving advantages. The states of Connecticut, Texas, Kansas, California and, recently, New York are among those who have started welded bridge construction.

Steel Ribs for a Heavyweight

The largest 3600-rpm generator ever built is being shipped from the General Electric turbine plant at Schenectady to the Joppa, Ill., generating station of Electric Energy, Inc. Welders are shown joining ribs and sides of a section of the stator of the unit.

Formed by five other Mid-Western utilities, Electric Energy, Inc., will supply electric power to the Paducah, Ky., plant of the Atomic Energy Commission which is now under construction.

Take the problem out of welding problem steels!

Use P&H Low-Hydrogen Electrodes

The right low-hydrogen electrode for your job!

70LA-2 (E6016, E7016)
General-purpose electrode for high-carbon, low-alloy, high-tensile, and high-sulfur free-machining steels.

70LB (E7016)
For repair welding of Grade B and other castings of similar analysis.

P&H .25C (E7016)
For castings repair where high carbon is preferred to alloys for raising tensile strength.

75LP (E8016)
For 2% to 3% nickel steels, both cast and rolled.

80LE (E8016)
For chrome-moly power piping and fittings.

90LE (E9016)
For higher-analysis chrome-moly power piping and fittings.

P&H .40C (E9015)
For .40 carbon castings and rolled steels where you want to match both chemical and mechanical properties.

AW2B (E9016)
For repair welding of cast armor, "off-analysis" steels, rerolled rail steels, and high-sulfur steels.

P&H # 7 (E9016)
For repair welding of Grade C and other castings of similar analysis.

90LH-2 (E10016)
For alloy steels, such as SAE 8630, 4130, and comparable types where the weld metal must match the heat-treating properties of the parent metal.

P&H # 12 (E10016)
For difficult fabricating jobs involving high-tensile and alloy steels with little or no preheat.

P&H # 17 (E12016)
For high-hardenable steels subject to high impacts through a wide temperature range.

P&H # 21 (E15016)
For aircraft and similar steels, such as 4340 and 4130.

. . . . and get high-strength welds that are free of underbead cracking

P&H has the most complete line of low-hydrogen electrodes in the industry — many "custom-made" to match the chemical analysis or heat-treating properties of a variety of alloy steels. This wide selection eliminates the difficulties in welding problem steels, such as high-sulfur free-machining, high-carbon, low-alloy, and high-strength steels.

The core wire of these electrodes is of a mild steel or a low alloy, thereby greatly reducing the cost of the low-hydrogen electrode, as compared to the higher-alloy types.

The coating ingredients of these P&H electrodes are mineral- and also hydrogen-free, with low moisture content. This eliminates underbead cracking and permits the welding of "difficult" steel with little or no preheating.

Other important advantages are: Better impact properties through a wider temperature range; Greater ductility at high tensile strengths; High-quality X-ray; As-welded properties that approach values ordinarily obtained only after stress relieving; More accurate control of alloying elements; No need for annealing before enameling; Higher heats for higher deposition rates.

Get better welds on "difficult" steels — cut your costs. Get P&H Low-Hydrogen Electrodes from your P&H representative or local distributor.

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Originally designed for gouging stainless steel, the Arcair Torch has found wide application among *fabricators* using all types of ferrous and nonferrous alloys. In *foundries*, the Arcair method is being applied with notable success to gouge defects and to remove unwanted metal. *Maintenance* of mine, quarry, railroad, construction and plant equipment provides other profitable uses.

The unique Arcair TORCH, with its air control valve, self-aligning rotating jet and concentric cable is all the equipment needed —complete at just **$87.50**

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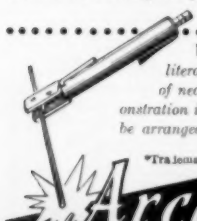

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Using a new G-E Alloy, Cond-Al, in field windings, the four generators of this type now on order will be in operation in 1954. Each has a rating of 216,000 kva.

New Film Shows Applications of Metallizing

A new 16-mm sound film in full color, recently completed by Metallizing Engineering Co., Inc., Long Island City, N. Y., describes the metallizing process and shows a wide range of practical applications as reported by many users throughout industry. Every scene in the film is taken in the plant of a metallizing user. Maintenance departments are shown metallizing worn or damaged machine parts of many different kinds. Users describe actual savings made possible by reduced repair costs, reduction of downtime spent waiting for replacements and simplification of spare part inventories. The film also pictures how thin coatings of comparatively expensive metals on ordinary base metals provide desired service characteristics at very low cost. The latest methods of surface preparation are demonstrated, as well as the use of metallized aluminum and zinc for the protection of iron and steel equipment and structures against corrosion.

The film opens with the observation that most industrial maintenance men would profit from a tour of plants throughout the nation, but since that is impractical, the film has been designed to do a thorough reporting job in its place. It shows a wide range of actual metallizing work being done on worn and damaged cracks, shafts of various kinds, inside diameters of different parts, press fits, all being restored to service with the metal spraying process. One of the jobs demonstrated in a large steel mill involves the restoration of backup rolls which cost $125,000 each to replace if new rolls have to be purchased.

Included among the applications for corrosion protection are storage tanks, bridges and trestles, degreasers and other steel work. One sequence shows the crack streamliner of the Missouri-Pacific, the "Sunshine Eagle," with 85,000 sq ft of underframes sprayed with pure aluminum for protection against corrosion. Power capacitor cases are shown being sprayed with pure zinc, a process that has replaced paint for many years on the Westinghouse production line.

Suitable for presentation to industrial, professional and educational groups, the purpose of the film is to promote a broader understanding of metallizing, to explain the principles on which it operates and to demonstrate its practical uses and profitable applications throughout industry. Running time: 28 min. For further information, contact R. J. McWaters, Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y.

New Aircomatic Film

"Tool of Many Uses," a new Kodachrome sound motion picture film just completed by Air Reduction, was premiered recently at the National Metal Exposition.

This new picture documents more than a dozen actual case histories of production line application of this newest welding process. The film includes aluminum welding operations on materials ranging from $\frac{1}{8}$ in. up to one full inch in thickness. It also shows aluminum welds 2 in. long that required only 7 sec to make, and aluminum build-up work in which 10 lb of metal were deposited in an hour's welding time. Copper welding shown in the picture includes one of the largest welded copper vessels ever built as a production item, aluminum bronze gear blanks 3 ft in diameter for marine use and other demonstrations of the versatility of the Aircomatic process. Both automatic and manual welding are shown in use on stainless and mild steel for a number of applications. Showing time: 18 min.

Engineering Awards

The results of the fifth Annual Engineering Undergraduate Award and Scholarship Design Program have been announced by The James F. Lincoln Arc Welding Foundation of Cleveland, Ohio. Engineering undergraduates in 23 different schools have been given national engineering recognition and awards totaling $5000. Scholarship funds of $1750 have also been awarded to three schools in honor of the first three award winners. Awards were made for the best designs of machinery and structures stressing the value of arc-welded construction.

First Award of $1129 went to Richard Cheverton and Thomas Musslewhite of Georgia Institute of Technology for their joint design of a subcaliber mount for a recoilless rifle. Army officials have expressed interest in the design which permits mounting a .22-caliber barrel on a 75-mm rifle for training purposes. In honor of the students, the Department of Mechanical Engineering of Georgia Tech received $1000 in scholarship funds.

Merle Geoffrion, as an undergraduate in the Civil Engineering Department of the University of Delaware, received the $564 Second Award for his design of a highway bridge. His Department received in his honor a scholarship fund of $500.

Third Award of $282 went to Robert Murray, a student in the Department of Architecture of Rensselaer Polytechnic Institute. His school also was awarded $250 in scholarship funds. Murray made an analysis to determine the economic span lengths of standard sections.

The program is sponsored annually by the Foundation to encourage engineering

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Your local Alcoa Distributor is a good man to know when new rearmament contracts require that you teach inexperienced workers how to weld and braze aluminum.

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BE COMFORTABLE with smooth-sitting DOCKSON GOGGLES. Excess weight is engineered out.

BE ECONOMICAL, get longer use from DOCKSON GOGGLES. "BUILT FOR BETTER SERVICE".

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students to study the possibilities of arc welding as an engineering tool for improving the quality of machinery and structures of all kinds while reducing costs. The sixth annual competition, now being sponsored, closes on June 29, 1953. All registered undergraduates are eligible to compete.

A. F. Davis, Secretary of the Foundation, states that in order to encourage participation by all engineering undergraduates, in whose hands future engineering rests, the rules of the program have been considerably simplified to avoid conflict with regular school work and the restrictions of time. The new rules booklet is available from the Foundation, Cleveland 17, Ohio.

De-STA-Co Clamps Maintain Holding Pressure at 500° F

Brooks and Perkins, Inc., Detroit, Mich., have adopted De-STA-Co Toggle Clamps as part of their stress relief fixtures for magnesium assemblies. Specifications of an army order on radar cabinets require stress relief of all extruded sections that are welded together to prevent the development of weld cracks in service. Distortion of the cabinet sections would disrupt the delicate electronic equipment of the radar sets making them useless.

When the extruded sections have been welded together with the equipment mounting blocks, and angle plates, the assembly is fastened to the fixture. Then it is rolled into a furnace where the temperature is brought up to 500° F for 1 hr before allowing to cool. The high temperatures have caused cracks to develop in the asbestos backing of the fixture but the clamps have retained their snap-action holding pressure.

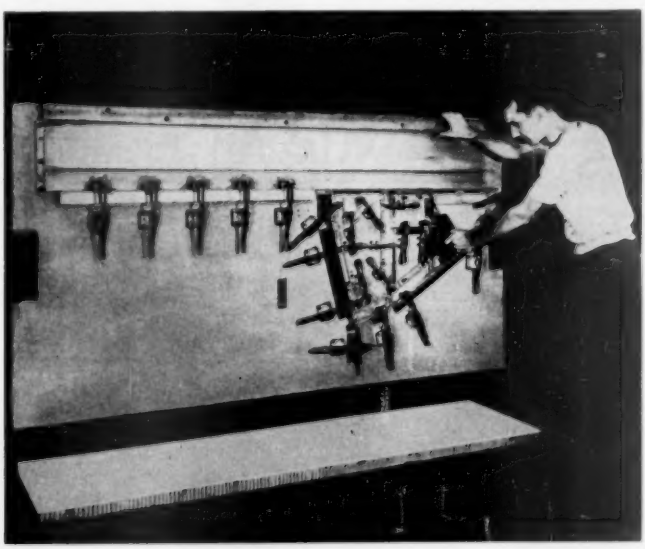
After the radar console cabinet is completed, it is again placed in the furnace for additional stress relief before shipment to its destination. The De-STA-Co Toggle Clamps are manufactured by the Detroit Stamping Co., 322 Midland Ave., Detroit 3, Mich.

50-Year Index to ASTM Technical Papers and Reports

This 50-Year Index provides a detailed author and subject index to all the ASTM technical papers and reports dealing with materials, particularly their properties and testing, appearing in ASTM publications covering the period from 1898 through 1950. From 1898 to 1902 the Society was known as the American Section of the International Association for Testing Materials. In 1902 the Society obtained its charter and was reorganized as the American Society for Testing Materials.

This index is intended to make readily available to the materials technologist, the researcher, the librarian, and all others concerned with the fields covered, pertinent information on a particular subject. Every effort has been made, after consultation with many users of indexes, to make available a ready means of locating information relative to a particular subject (or author) with a minimum expenditure of time and effort, without making the index too unwieldly.

The index is concerned basically with technical papers which have appeared in *Proceedings, ASTM Bulletin*, Special Tech-



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and RADIOGRAPHY adds "OK"

Here you see the lower drum of a large stationary boiler. All seams of the shell are welded. So are the extensions and elbows. And every weld has been radiographed to prove it sound. For radiography would reveal any serious lack of fusion or gas porosities.

Knowing each weld to be sound, the designer of a pressure vessel can save weight, cut cost, and improve its efficiency.

This is why the use of radiography is widening the opportunities of welding—why it can increase your business.

If you would like to know more about how it can help you, get in touch with your x-ray dealer and talk it over.

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X-ray Division, Rochester 4, New York

RADIOGRAPHY...

another important function of photography

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TRADE-MARK

nical Publications or special compilations of standards.

Certain innovations are embodied in this index; for example, the author index which precedes the subject listings gives the complete titles of papers and items covered.

Copies of this 216-page publication, in red cloth binding, 6 by 9 in. page size, can be procured from ASTM Headquarters, 1916 Race St., Philadelphia 3, Pa., at $6.00 per copy.

$6750 Engineering Undergraduate Award Program

The Rules and Conditions for the sixth annual competition of the Engineering Undergraduate Award Program have been released by The James F. Lincoln Arc Welding Foundation. The Rules for this year's competition have been changed in a number of important respects: (1) The 46 awards to be made will be for the best designs of a machine, machine component, structure or structural part that have been designed for welded construction. (2) Duplicate awards will be made for the best entries in both mechanical design and structural design. Additionally, three grand awards will be made to the best of the Program designs. (3) To permit participation in the Program within the restriction of time available in the normal curriculum, all entries will be limited to no more than 20 pages. Undergraduates will also be permitted to complete their entry for the Program after graduation.

The Rules and Conditions booklet, now available from the Lincoln Foundation, Cleveland 17, Ohio, gives suggestions for subject matter, a bibliography, and previous award titles illustrated with drawings.

$7000 High-School Award Program

The Rules and Conditions booklet for the $7000 Arc Welding Award Program for high-school students on farms and ranches is now available from the Lincoln Foundation, Cleveland 17, Ohio.

This is the third program sponsored by the Foundation for high-school students to encourage them to study how arc welding can contribute to better farm operation and maintenance. The Program stimulates interest in farm shop work, and especially in welding as used there. One hundred cash awards totalling $5000 are offered to students for the best descriptions of how arc welding is or can be used in making and repairing farm equipment and tools. Awards are to individual students or a group of students. Both home and class projects are eligible. Ten awards, totaling $2000, are also made to schools for the improvement of shop mechanics courses.

All high-school students living on farms or ranches are eligible to compete. The Rules can be obtained by writing to The James F. Lincoln Arc Welding Foundation, Cleveland 17, Ohio. The Rules booklet contains 61 pictures of tools and equipment made by high-school students for which awards have been made in previous programs.

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Are you still using costly silver solders to braze copper, brass or bronze? The time-tested Phos-Copper and the sensational new Phos-Silver will braze these metals at a fraction of the cost of silver solder and do the job better and faster with these advantages:

No Flux: On copper-to-copper joints, a flux is not required when using Phos-Copper or Phos-Silver.

Low Temperatures: Phos-Copper permits brazing temperatures as low as 1350° F. Where even lower temperatures are required, Phos-Silver may be used as low as 1225° F.

Perfect Bonds: The high fluidity and excellent wetting properties of Phos-Copper and Phos-Silver assure strong, ductile bonds capable of withstanding repeated shock and vibration.

For further information call your nearest Westinghouse office or write Westinghouse Electric Corporation, Welding Division, Buffalo, N. Y. Ask for Brazing Booklet B-5454. J-21712

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WELDING CONNECTORS

Saxe Welding Connection Units position and secure structural parts to be welded.

Clip K3A permits an adjustable connection.

These widely used units eliminate all hole punching, and, with welding, produce the most economical, safe, and quickly erected structural frame.

Write for 1951 edition, Structural Welding Practice Manual.

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today? yes sir!

When you need welding supplies in a hurry—contact BURDOX. Complete stocks of industrial gases, welding equipment and safety equipment are carried to meet emergency requirements. One call delivers all your needs.

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BURDOX

Carrying hot steel for THREE YEARS

and these Hard-Faced rollers are still in use

Three years ago, some cast iron cone rollers for this cooling bed were hard-faced with HAYNES alloy No. 92. Today, they are still carrying hot steel bars despite the continual impact, abrasion, and heat to which they have been subjected. Cast steel rollers on the same line, which were hard-faced with HAYNES STELLITE alloy No. 6, have also shown exceptional service — although they have not yet been in use as long as the cast iron ones.

HAYNES alloy No. 92, an iron-base alloy recommended for the cast iron parts, is exceptionally easy to apply by oxy-acetylene welding. It has a very low melting point and the molten alloy is quite fluid. Deposits have a Rockwell C hardness of 62-67 and excellent wear resistance.

HAYNES STELLITE alloy No. 6, the cobalt-base alloy used for hard-facing the cast steel parts, can be applied by metallic arc welding. Deposits are machinable and have excellent resistance to abrasion and heat.

For information on how to hard-face cast iron and cast steel parts, and on the alloys recommended for each job, write for a copy of "HAYNES Hard-Facing Manual."

HAYNES TRADE-MARK *alloys*

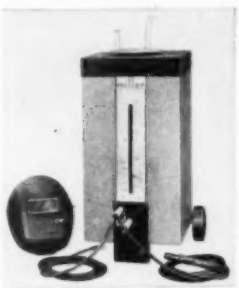
"Haynes" and "Haynes Stellite" are trade-marks of Union Carbide and Carbon Corporation.

Haynes Stellite Company
A Division of
Union Carbide and Carbon Corporation
UCC
General Offices and Works, Kokomo, Indiana
Sales Offices
Chicago — Cleveland — Detroit — Houston
Los Angeles — New York — San Francisco — Tulsa

NEW PRODUCTS

Small Welder

Here is a small electric welder that is ideal for home welding, farm use, small shops, etc., and has the same welding characteristic features as the large a-c transformer welders—designed for use on standard 110 or 220 power supply lines—will handle electrodes from $^1/_{16}$ to $^5/_{32}$ in. (when on 110 v the maximum is $^1/_8$-in. electrode). Incorporating the same principle used in the larger industrial welders, it has movable primary coils which permit an infinite current adjustment throughout



the entire welding range. Smooth resettable current control is obtained through the adjustable hand crank mounted on the top of the welder; it has two welding ranges—high and low. Welding amperage is determined by the amperage scale on the front. In its construction, the primary coils are air spaced with glass-covered magnetic wire. Enclosed in a heavy steel case, it weighs 156 lb, equipped with two rubber "oilite" bearing 5-in. wheels and handle so that it can be easily moved from one location to another, and comes equipped with detachable welding cables with jack plugs. Manufactured by Miller Electric Manufacturing Co., Appleton, Wis.

Heliarc Welding Torch

A new water-cooled Heliarc torch for inert gas-shielded arc welding is announced by Linde Air Products Co., a Division of Union Carbide and Carbon Corp. The torch has a rated capacity for continuous duty of 300 amp., and either high-frequency stabilized alternating current or straight polarity direct current can be used. This new torch, the HW-10, is designed for welding practically all commercial metals up to about $^1/_4$ in. thick.

The HW-10 has a torch head and handle assembly weighing only 12 oz. Even if it is used for long periods of time at full rated current, it will not overheat. Cooling water flows directly into the torch body—there is no "outside plumbing." Water is circulated to the torch head, through the water jacket, back through the head again, and out through the power cable.

This improved water-cooling system makes possible a design feature that minimizes electrode stub loss. Since the electrode holder is provided with direct water cooling, the collet body is brought closer to the arc without damage from excess heat. With the electrode holder brought down closer to the arc, electrode stub loss is reduced.

For further information about the Heliarc HW-10 welding torch, see your local Linde representative, or write to Linde Air Products Co., 30 E. 42nd St., New York 17, N. Y.

Copper Electrode

Krembs & Co., Chicago, have perfected a shielded-copper welding electrode and welding procedure for metallic arc welding copper and all copper alloys as well as cast iron and steel.

"Kop-R-Arc" makes strong and ductile welds on all copper alloys, also steel, cast iron and standard steel. A free bulletin describing this new process will be sent those writing Department K, Krembs & Co., 669 W. Ohio St., Chicago 10, Ill.

"Chemalloy" Welding Rod and Solder

Chemalloy is a new development which revolutionizes and simplifies the art of welding or soldering any grade of aluminum as well as any zinc-base metal such as pot metal or white metal.

It requires no flux, no special cleaning of preparation of metal, no special welding equipment, no special skill. Merely heat metal beyond 800° F and rub on. It flows through the skin of aluminum and makes a weld stronger than the aluminum itself. Will in turn take soft solder and permit attachment of radio-electronic or electrical wires to aluminum. It is produced by blending several metals and chemicals to yield a very homogeneous and finely grained metal. Melts and flows easily without splatter and free of lumps and irregularities.

Useful in every airplane, electronic, aluminum roofing, garage, foundry casting and automotive plant, or repair facility. Also ideal to repair damaged automobile radiator grilles, cracked carburetors, fuel pumps, automotive ornaments and accessories and aluminum utensils. Defies rust, corrosion, exposure and salt water.

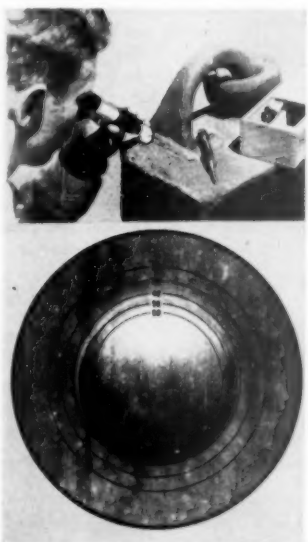
Write Sightmaster Corp., 111 Cedar St., New Rochelle, N. Y.

Portable Hardness Tester

Pacific Transducer Corp., of 11921 W. Pico Blvd., Los Angeles 64, Calif., offers a new low-cost portable pocket hardness tester for determining the hardness of steel alloys and other metals in the range of from 25 to 65 Rockwell "C" scale.

The test set features portability. It can be carried in the pocket on the job. It may be used to test specimens of steel alloy in almost any place or position. It is a direct reading device. No further calculations are required. This makes the unit particularly fast in operation.

The test includes a microball indentor, a measuring microscope and a standard hardness test block. It operates by means of an impact indentor which drives a $^1/_{16}$-in. tungsten carbide ball a short distance into the sample to be tested. The diameter of the indentation is then measured by the reticulated microscope. As the diameter is a function of the hardness, the reticle indicates the hardness directly. It is accurate $\pm 1^1/_2$ points "C" scale.



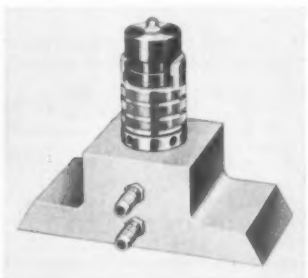
It lends itself particularly to use by field engineers, maintenance engineers, sales engineers and plant engineers. It is particularly adapted for testing all parts of large dies, punches and large welds as well as for structural steel, tanks and large pipes in position where only a portable hand tester of this type can be used. The net price of the unit is $69.

Die Adaptor

The same die can be used for many different resistance welding operations with the "Nu-Twist" Die Adaptor now being manufactured by P. R. Mallory & Co. Inc., Indianapolis. A quick change of electrode inserts is all that is necessary to adapt the die to various operations such as spot welding, projection welding, electrical upsetting and electro-brazing.

The unique design of the adaptor holder and electrode inserts, having no threads or tapers, enables the operator to release or secure the electrode with a simple turn of the locking nut so that he can change inserts while the die base remains in the machine. A clearance space of only 1/16 in. is ample between the upper and lower electrode inserts when replacement is made.

Because it cuts to seconds the time necessary to change a welding machine die setup from one operation to another, the Mallory "Nu-Twist" Die Adaptor is particularly suitable for short-run resistance welding applications.



Mallory "Nu-Twist" Die Adaptor bases are custom-designed to fit the machine specifications. "Nu-Twist" Electrode Inserts come in a range of sizes to fit most welding applications, but can be designed and constructed to fit special needs if necessary.

"Nu-Twist" Electrode Inserts are made from Mallory alloy bar stock, possess optimum physical and mechanical properties, are simple in construction and low in cost.

Other special features of the Mallory "Nu-Twist" Die Adaptor are the manually operated locking nut requiring no tools; the unique double-groove construction in bore of locking nut which positively aligns insert centrally with die adaptor and locks electrode insert in position when nut is tightened; the Rubber "O" Ring Seal which provides water-tight connection at all times; the passages for fluid coolant which are properly proportioned to insure efficient cooling of electrode insert and die adaptor. All components are of corrosion-resistant alloys, assuring complete protection from coolant stoppage, rust and corrosion.

Complete technical information available on request: Bulletin No. 8-22, P. R. Mallory & Co., Inc., 3029 E. Washington St., Indianapolis 6, Ind.

Manual Gun for Aircomatic Welding

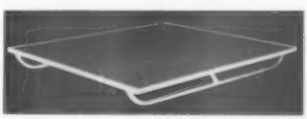
The new model No. 20 Aircomatic Gun, the latest development for manual inert-gas-shielded metal-arc welding has been announced by Air Reduction.

This manual gun, designed to operate with currents up to 500 amp, is recommended for welding copper, bronzes, stainless steel, nickel and steel. The use of internal water cooling of the gun and a water-cooled welding cable permit high current capacity without sacrificing lightness and flexibility of the gun.

For further information write to your nearest Airco office or to Air Reduction, 60 E. 42nd St., New York 17, N. Y.

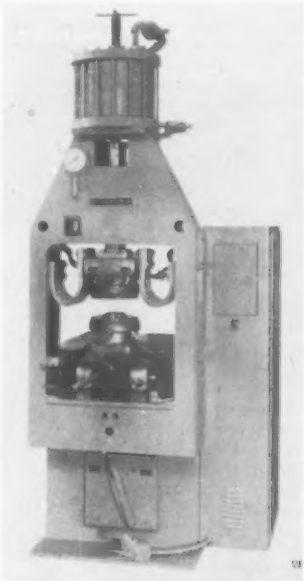
Pallet

New tubular steel all-purpose pallet, introduced by the Econoweld Co., 1805 Webster St., Dayton, Ohio, is lighter than wood pallets of the same dimensions and load capacity, but priced in the same brackets. Shown is one of many types which depart radically from conventional pallet design (patent applied for). The eight-entry heavy-duty product has tubular steel runners weld-braced to an angle-iron bound deck. Manufacturers' use to date indicates ten times the serviceable life of wood pallets with added ease and safety in handling. The maker announced the new pallets will be built to the customer's specification without added cost.



Low-Power Demand Welder

The Taylor-Winfield Corp. of Warren, Ohio, has developed a new projection welder of low-power demand. This resistance welder is designed to weld reinforcements in automotive brake drums. It is new and unique in its low-power demand for heavy-duty multiple projection welding.



Midget Type X-ray Machine

A new midget-type industrial 250,000-x-ray machine, less than half the size and less than 1/4 the weight of the conventional quarter-million-volt unit, was exhibited by General Electric at the 34th Annual National Metals Exposition.

This lightweight, mobile unit, although capable of X-raying steel up to 3 1/2 in. in thickness, can be easily carried around in foundries, welding shops, shipyards, building projects and on many other jobs where X-ray inspections are needed to control quality and safety.

The unit is less than 15 in. in diameter and 44 in. long; weighs only 150 lb, as against 1150 for conventional units.

The first major application, according to General Electric, has been made in U. S. Navy shipyards where it is used to inspect critical welded seams and stressed areas in ships during and after construction.

A feature of the unit's versatility is its protruding "snout" from which the X-rays are emitted, which makes possible the taking of "inside-out" X-ray pictures. This, according to G-E engineers, will greatly speed up the process and reduce problems now faced in making X-rays.

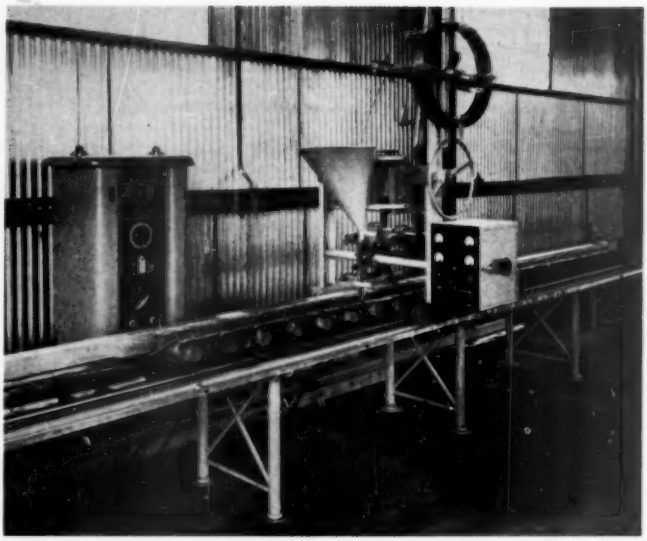
In setting up for the inspection of a weld joint connecting two sections of a pipe, for example, the X-ray technician can bring the X-ray unit inside the pipe and change its position for each exposure area without disturbing the pipe. He can also use it inside large castings and other areas difficult of access, or insert the snout inside a smaller casting.

Operating at anywhere from 75,000 to 250,000 v, the new unit can be used on anything from magnesium to steel. Use of the machine on light metals is also aided by the beryllium "window" of the X-ray tube, which allows the escape of softer, less penetrating X-rays from the tube.

Tuna Cookers

The photograph shows the largest battery of Tuna Cookers in the world—double-door, double-row units for 30 racks each—capable of handling between 100 and 150 tons of fish per cook depending on the size of the fish.

These units were fabricated in the plant of Atlas Welding & Manufacturing Co., 2200 W. Pacific Coast Highway, Long Beach 10, Calif., and delivered to a local Los Angeles Harbor Cannery. They are of all-welded construction, 6 x 7 ft and 42 ft 6 in. long with counterbalanced, bolted-type doors.



Automatic Welder-Positioner Rebuilds Crawler Track Rails

A fully automatic welder-positioner for crawler track rail rebuilding and other longitudinal welding is now in production by Dean Weld Machine, Inc., of Oakland, Calif.

Principal advantages claimed for the machine include faster setup, negligible distortion, fully automatic operation, and fast, economical operation. Crawler tracks do not have to be disassembled for rail rebuilding; the 40-ft positioner bed will accommodate a full-length track. Low heat required reduces distortion so that track pins may be turned after welding. Automatic controls break and restart the arc at each link joint so that build-up metal does not fill the rail joints cutting or grinding after welding is virtually eliminated.

Experience with several units now in operation indicate that the typical 76-link track can be rebuilt in one 8-hr day with a single operator, including setup and finishing time.

The machine also handles longitudinal welding such as box-beam fabrication, scraper blade and bulldozer lip rebuilding, and welding or build-up on any sections which can be positioned in the 40-ft by 28-in. welding bed. Extra 10-ft bed extensions are available.

The unit is shipped complete with bed, carriage, welding head, 500-amp transformer and all controls and cables.

Literature and prices may be obtained from the manufacturer, Dean Weld Machine, Inc., 10154 Edes Ave., Oakland, Calif.

Automatic Heliweld Hard-Facing Process

A new automatic hard-facing process, using the standard Automatic Heliweld Head and a unit for feeding bulk tungsten carbide, has been announced by Air Reduction.

Growing out of a need for improved methods in depositing tungsten carbide hard-facing material on tool joints for well drilling rigs, Airco has developed an Automatic Heliweld Hardfacing Unit. Its flexibility has been proved by numerous tests which show that its use for the deposition of tungsten carbide hard-facing material is limited only by the ability to position the work and the arc in the proper relationship to each other.

The manner of deposition of loose, granular, pure tungsten carbide particle, minimizes reduction of particle size by solution of the tungsten carbide into the base metal. It also minimizes the heat-affected area adjacent to the deposit.

In operation, the Heliweld arc melts the base metal, producing an elongated pool

Tuna Cookers

The Greatest Advancement in D.C. Welding!

THE NEW
A.O.Smith
RECTIFIER

Available in 200-, 300- and 400-Amp. production welding models, N. E. M. A. rated.

OUTPERFORMS ANY MOTOR GENERATOR SET MADE!

No moving parts to wear out. No brushes and bearings to replace. No commutator to corrode or turn down . . . maintenance expense practically eliminated.

Instant striking and smoother arc! There's no inductive time lag usually found in motor generator sets.

OUTPERFORMS ANY OTHER RECTIFIER MADE!

Eliminates stack failure. High-velocity, down-draft ventilation provides plenty of cooling air within the machine. There's no need for thermostatic protection, no interruptions because of current cut out! Even when short circuited while set at rated output, rectifier-stack temperature rise is held below critical levels!

Eliminates machine noise and current creep. No large magnetic forces develop to cause vibration or tear the machine apart, thanks to its moving-coil design. And positive air separation between primary and secondary coils guards against insulation failure which otherwise might put line voltage on welding leads.

If your jobs call for DC welding . . . you'll weld faster, better, at *less* cost with this new A. O. Smith D. C. rectifier welder—an entirely new concept in D. C. production welding!

For complete information and name of nearest distributor or A. O. Smith representative, write:

A. O. SMITH CORPORATION
WELDING PRODUCTS DIVISION
Dept. WJ-1252, Milwaukee 1, Wisconsin
International Division: P. O. Box 2023 Milwaukee 1, Wisconsin, U. S. A.

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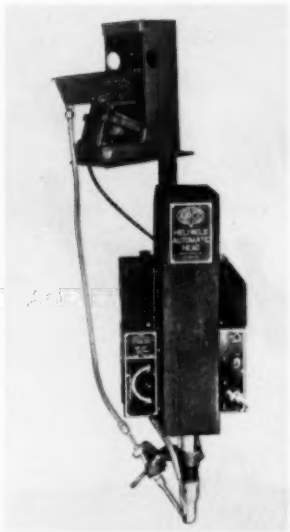

Fig. 1 Basic Automatic Heliweld Hardfacing Unit: Automatic Heliweld Head and Vibratory Hopper Assembly

Fig. 2 Feeding tungsten carbide particles into the pool behind automatic Heliweld arc

and the tungsten carbide particles are poured into this pool behind the arc. The rate of tungsten carbide feed from the Vibratory Hopper mounted on the Airco-

Fig. 3 Good distribution of correct particle size and narrow heat-affected zone are evident in this section of a deposit on a tool joint

matic Heliweld Head is electrically controlled with a high degree of accuracy.

Tests have shown that the abrasion resistance of deposits made by this process is considerably greater than that attainable with other arc methods.

Arc Welder

Featuring magnetic amplifier principle, an integral part of the welder itself, is the new Miller Selenium Rectifier D. C. Arc Welder that combines all the advantages of the a-c transformer type welders and the d-c motor generator welders, just announced to the industry. This new welder gives high short-circuiting current that provides a pulsating d-c current which insures faster electrode deposition and sounder, denser welds. Utilizing advantages heretofore considered impossible, the manufacturer states that these new Selenium Rectifier Type D. C. Arc Welders open up an entirely new field in electric welding.

Exclusive electric control, making remote operation simple and convenient. Available in four sizes specifically designed for all d-c welding applications. Illustrated is the Model 100, designed for light d-c applications where low amperages and crater elimination are a "must." Other sizes range up to Model 400 designed for heavy production welding. Designed and manufactured by Miller Electric Manufacturing Co., Appleton, Wis.

Engine-Driven D-C Arc Welder

A new 200-amp, engine-driven d-c arc welder with a 60% duty cycle has been announced by the General Electric Co.'s Welding Department.

Designated as Type WD-42AGW, the new welder has a current range of 40 to 250 amp, and can be used with a variety of electrode sizes for repair, maintenance and construction work.

Consisting basically of a G-E type WD42 generator and a Wisconsin air-cooled engine, it is designed to fit crosswise in a standard pick-up truck. Optional equipment includes a trailer equipped with fittings for pressure lubrication.

Forced ventilation keeps internal temperature within safety limits when the welder is operated on a 60% duty cycle at rated load. In addition, tests indicate

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No. 4 Braz-Cast Flux for Bronze Welding Cast Iron
No. 5 & 8 Cast & Sheet Aluminum Fluxes
No. 9 Stainless Steel Welding Flux
No. 11 Tinning Compound
No. 16 Silver Solder Paste Flux

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PEDESTAL TYPE
MODELS WM-629 • WM-680
SPOT WELDERS

"THE STRONGEST IN YOUR PRODUCTION CHAIN"

WM-629 SPECIFICATIONS

TRANSFORMER: 220 or 440 volt primary as specified; 3.19 minimum to 5.0 maximum secondary voltage. Four Point Dial Type Heat Selector Switch, mounted for outside adjustment. Water-cooled secondaries.

ARMS: 2½" dia., mounted in heavily ribbed copper castings, equipped with standard water-cooled 1" dia. point holders.

THROAT: 12" to 18", stroke, 3".

OPERATION: 3" dia. air cylinder. ⅜" four-way solenoid air valve operated through foot switch.

FRAME: All-welded steel construction. 60" high, 25" wide, 25" long.

SHIPPING WEIGHT: 800 lbs.

WM-680 (shown on left) SPECIFICATIONS

CAPACITY: 75 KVA at 50% duty cycle.

TRANSFORMER: 220 or 440 volt primary as specified; 6.6 minimum to 10. maximum secondary voltage. Six Point Dial Type Heat Selector Switch, mounted for outside adjustment. Water-cooled secondaries.

ARMS: 3" dia., mounted in heavily ribbed copper castings. Standard water-cooled 1¼" dia. point holders.

THROAT: 18"; stroke, 3".

OPERATION: 5" dia. air cylinder; ⅜" four-way solenoid air valve operated through foot switch.

FRAME: All-welded steel construction. 70" high, 30" wide, 60" long.

SHIPPING WEIGHT: 1100 lbs.

Note: WM-680 also available with Transformer capacity of 70 KVA at 50% duty cycle; 5.0 minimum to 7.1 maximum secondary voltage. All other specifications as indicated above.

LINK offers the engineering advances that mean ACCURATE, HIGH SPEED WELDING PRODUCTION to you!

Ideal for high speed resistance welding of mild steels in broad variety, these units have features that assure extreme accuracy in volume production. Every Link model is engineered to JIC electrical and air standards. Moderate in original and low in operating cost, these Link Spot Welders will perform dependably for you under any production schedules.

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LINK WELDER CORPORATION

Designers and Manufacturers of High Production Welding Equipment

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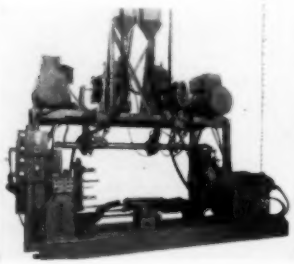
that heavy overloads for short periods do not produce harmful results.

Maintenance-wise, either the generator or engine can be removed easily for overhaul and replaced by a standby unit. Since the engine is air cooled, coolants or antifreezes are not required.

A vacuum-type device saves gasoline by idling the engine when welding is not being done.

Press and Girth Welder

The photo shows the girth-welding machine developed by Morten Manufacturing Co., Muskegon Heights, Mich., for submerged-arc and inert-arc tank welding production. The machine is engineered to make fillet and burn-through circumferential welds. The main feature of this machine is a built-in 5-ton hydraulic press for the assembly of parts prior to welding. A two-pressure hydraulic system is incorporated with high pressure for assembly and low for holding parts during welding.



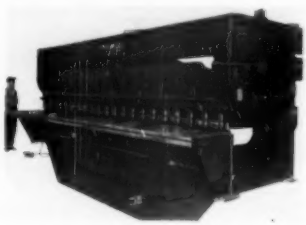
The stationary headstock supports the center spindle drive and gives reaction to the press while the adjustable tailstock houses the hydraulic press cylinder. The press is under push button inching control and the eject and reload cycle is automatic in operation. Manual longitudinal and radial positioning of the welding heads is provided to cover the machine range.

Less than half an hour changeover time is required, keeping nonproductive hours at a minimum.

The machine has a capacity of 9 to 24 in., 12 to 36 in. or 18 to 48 in. diam and up to 96 in. long.

New Steelweld Shear Cuts 20-Ft. Plate

New Series 3C20 Steelweld Shear, made by The Cleveland Crane & Engineering Co., Wickliffe, Ohio, is the longest to date, with a shearing capacity of 20-ft by $^{3}/_{16}$-in. steel plate.



The frame is solidly built with all sections of steel plate welded, thus assuring nonmovement of parts and life-time alignment. The bed, end housings and crown are all welded together into one integral unit. Because of the great length the bed extends below the floor to prevent deflection.

Employment Service Bulletin

Position Vacant

V-280. Welding Engineer—Supervisor. Must have working knowledge of manual and automatic welding of carbon and alloy steels, plate and sheet products—inert arc and resistance welding—ability to write welding specifications and procedures. Western Pennsylvania.

V-281. Welding Research Engineer. A company which has pioneered in welding and is considered by industry as a leader in the field has an excellent opening for a welding engineer with experience in resistance welding. This is an interesting position in the development of welding application to diversified projects. If you are interested in doing resistance welding research and development, your inquiry is invited.

V-282. Sales Manager. Experienced in arc welding equipment or power and lighting transformer field. Responsible for planning and executing sales, maintaining and building a factory sales representative organization, sales promotion and advertising. Travel required. Good salary, expense account and bonus. Small, fast-growing, well-established organization manufacturing competitively priced distribution transformers type arc welding equipment sold nationally through manufacturers agents to jobbers and dealers. Challenging opportunity. Give full details and photo. Confidential.

Services Available

A-631. Welding Metallurgist—Technical Service. B.S. in Met.E., U. of Michigan. Age 35, married. Five years' experience in technical service and sales, customer complaint and contact work in welding and metallurgy. Six years of development, procedures and specifications in welding, metallurgy and steel mill trouble shooting. Considerable public speaking before AWS sections, plant staffs and sales meetings. Published technical articles and taught college metallurgy.

A-632. Welding Engineer. Presently employed. Age 31. Engineering graduate with ten years' experience in shipbuilding, oil refinery pressure vessels, power plant equipment fields. Desires supervisory position in welding production, engineering or sales.

Excellent Opportunity available for

RESISTANCE WELDING ENGINEER

Electrical Engineering Degree and two (2) years' experience in qualification of equipment, establishment and certification of aircraft welding schedules, or recent graduate specializing in the field of Resistance Welding.

DOUGLAS AIRCRAFT COMPANY, INC.

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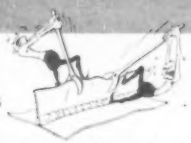
Low Temperature EUTECTIC WELDING ALLOYS

stops mild steel welding failures

minimize stress  ...distortion ...warpage with Low Temperature EUTECTIC WELDING ALLOYS

impossible?...that's what the Stolper Steel Products Corporation thought, too, until they actually tried these **new,** entirely different EUTECTIC "Low Amp"® EutecTrodes.® Then they came up with the astounding Case History shown below...Yes, we not only **claim** this amazing performance...we **prove** that "Eutectic" is different...prove that these new alloys **solve** metal-joining problems **where conventional materials fail!**...Let us prove it in your shop...just as we have in over **78,000** other plants and shops throughout America...There's no cost, no obligation involved...just fill in and **mail** the coupon below for a **free** consultation-demonstration from one of our 350 trained District Engineers who stand ready to serve you from coast to coast.

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DEVOTED TO THE RESEARCH AND MANUFACTURE OF
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Not only "MILD" steel, but also high carbon steel; cast steel; high-speed steel; manganese steel; stainless and all alloy steels; structural steel; aircraft steel, etc., in addition to all other metals from "A" for Aluminum to "Z" for Zinc can be joined faster, better, and more economically with one of the more than 150 different "Eutectic Low Temperature Welding Alloys".(All arc, torch and furnace applications.)

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TIME SAVINGS PER 100 TANKS!

Airtack and weld complete..43.7%
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"This reflects a savings of 43.85 hours (40%) per 100 tanks. The comparisons were made on production runs and the hours per 100 tanks shown are actual time study standards used for incentive purposes.

"I wish to thank you for the splendid cooperation which made this savings possible." —*L. L. D., Ind. Eng., Stolper Steel Products Corp.*

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WJ-12

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PORTLAND, OREGON • PITTSBURGH, PA. • HOUSTON, TEXAS • MILWAUKEE, WISC.

NEW LITERATURE

Hobart Gas Drive Arc Welders

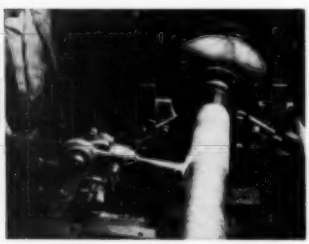
A new 8-page folder illustrating and describing the complete line of Hobart Gasoline Engine Driven Arc Welders is being mailed by Hobart Brothers Co., Troy, Ohio.

Complete electrical and mechanical specifications are given for the arc welders and combination model of arc welder and power unit. A free copy will be mailed to anyone on request to Hobart Brothers Co., Troy, Ohio.

Piston Salvage

A West Virginia machine shop reports on an interesting metallizing job involving the salvage of compressor pistons for a large chemical corporation. Rods are $5^1/_4$-in. alloy steel and wear $2^1/_2$ ft on either side of the centrally located 19-in. pistons.

Here is how the job was done. The rods are first turned down approximately 0.100-in. on the radius, threaded with a fine U-type thread and then prepared with a special surface roughing tool, which produces a surface somewhat similar to knurling.



The prepared surface is then sprayed with a special stainless alloy to about $^1/_{32}$-in. over the original diameter. Rod is then ground to required dimension.

Replacement cost for these piston rods runs about $1000 to $1200 each, with delivery very uncertain. Metallizing puts a unit back in service in two days and costs about $250.

The chemical corporation for whom this work was done, has found that a rod with a metallized surface is superior to the original rod. Its useful service life is greater and the packing wears longer. As a result of these advantages, they now have new piston and rod units metallized before installation.

This application and several other "money-saving" metallizing applications are described and illustrated in the *Metco News*, Vol. 6, No. 4. Just write for your copy to Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y.

New Aircomatic Catalog

A new, 12-page, two-color booklet describing the equipment and supplies employed in Aircomatic welding, Airco's inert-gas-shielded metal-arc process has just been published by Air Reduction.

This profusely illustrated booklet covers in detail both the No. 3 and the new No. 20 manual, *Aircomatic Guns and the Aircomatic Head*, for automatic operation. Aircomatic wires for use with this equipment also are covered.

To obtain your copy of this new Aircomatic Catalog (Form ADC 717 B), write to your nearest Airco office or to Air Reduction, 60 E. 42nd St., New York 17, N. Y.

Wall Chart Now Available

Just published is a wall-chart 18x27 in., designed to aid welders, on the job, to select the particular All-State alloy and flux which will best serve the welding, brazing, soldering, cutting or tinning job at hand and at the least cost. Data on 41 alloys and appropriate fluxes are arranged so as to make accurate selection as nearly automatic as possible. All All-State alloys and fluxes are covered according to the metal upon which work is to be performed. Copies may be obtained from any All-State distributor and from All-State Welding Alloys Co., Inc., 249–55 Ferris Ave., White Plains, N. Y.

"Nickel-Arc 60" Electrodes

Alloy Rods Co., York, Pa., issued a new 4-page bulletin on their new *Nickel-Arc 60* electrodes for machinable welds on cast iron. This is their Bulletin AR52-2218.

Cutting Torch

The M38 Catalog Sheet has just been released by the K. G. Equipment Co., 13-11 44th Ave., Long Island City, N. Y., and is available on request from the above manufacturer.

Fatigue and Fracture of Metals

Edited by William M. Murray, with Foreword by Jerome C. Hunsaker, *Fatigue and Fracture of Metals* contains 14 papers written by recognized authorities for the purpose of focusing our present knowledge of this vitally important engineering problem and of indicating paths for future research.

The discussion embraced general experience with failure of metals, specific fields in which fatigue occurs, the internal mechanisms probably involved in fatigue damage, the significance of various metallurgical phenomena to fatigue, the potential usefulness of different research methods to disclose more about the mechanism itself and about ways of countering it in design, and the direction of future research as forecast by the speakers' specific recommendations.

This book is published by John Wiley & Sons, Inc., 440 Fourth Ave., New York 16, N. Y. The price is $6.00 per copy.

Guide to Silver Brazing

If the more-than-100,000 requests for the American Platinum Works Manual *A Complete Guide to Successful Silver Brazing* may be used as a yardstick to measure the growth of interest in low-temperature silver brazing, and it surely can, then silver brazing is fast reaching its important place in industry.

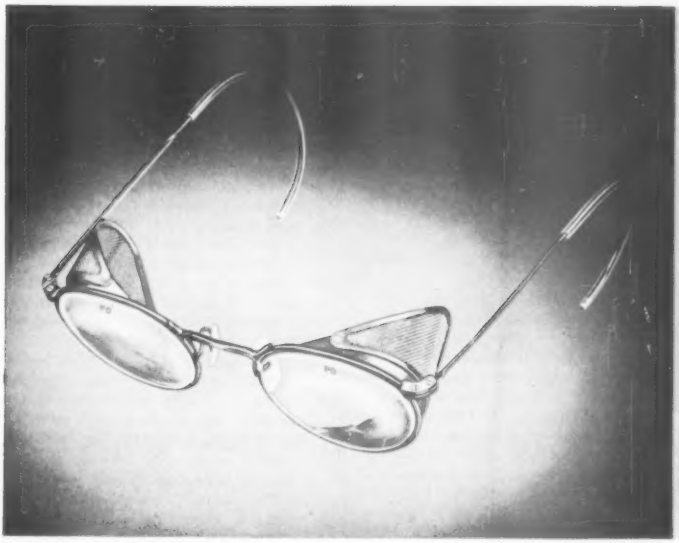
The booklet is the most complete guide available for all aspects of silver brazing applications and procedures. It provides correct answers to all general questions on low-temperature silver brazing, brazing alloys, joint design, preformed brazing shapes, ply metals, fluxing, heating methods, cleaning and inspection. More than 50 drawings and charts are used to illustrate the text. Several of the reference charts have been found to be particularly useful in selecting specifications for silver brazing alloys, to check U. S. Government Specifications and for Conversion Data. There is also a specially designed graph that is used to calculate quickly the thermal expansion of metals at the various silver brazing temperatures.

Copies of Silvaloy's *A Complete Guide to Successful Silver Brazing* are again available without charge to engineering and brazing personnel of industrial manufacturers, libraries, schools, etc., upon request to The American Platinum Works, 231 New Jersey Railroad Ave., Newark 5, N. J.

Cutting Metals

How to gouge and cut all metals with only carbon arc and compressed air is described in a new brochure on the *Arcair Torch*, available on request from the Arcair Co., 2614 Burwell St., Bremerton, Wash.

PENNSYLVANIA OPTICAL COMPANY



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The new 220 Series Goggles are designed to give the utmost in eye protection. Comfort is assured during continued wear by proper balance combined with lightness of weight. This results in worker approval and a higher factor of safety. Varied combinations make these goggles adaptable to many industrial operations requiring positive eye protection.

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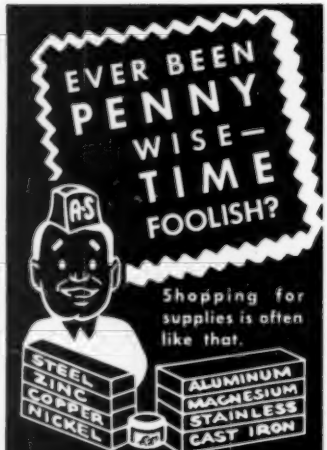

Now is the time to investigate the important services your nearby All-State Distributor offers.

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It makes sense to make your A-S Distributor and All-State your one proven source for all alloys and fluxes needed for welding, brazing, soldering, tinning and cutting.

2. BETTER JOBS FOR LESS

A-S Distributors stock a large variety of things you have to have quickly when you want them. They're competitive on price — usually lower if your eye is on the job costs. That comes of their handling All-State Alloys and Fluxes for all metals. They do better jobs for less.

3. TECHNICAL SERVICE

Each All-State Distributor has men especially qualified by training and experience to aid in the proper selection and use of alloys and fluxes for jobs where you might need help. Back of them, and always ready to pitch in on the problem jobs are the All-State regional men and the well-known All-State technical service facilities at White Plains, N. Y.

4. FREE LITERATURE

Ask for 32-page Buyers Guide to the complete line of All-State Alloys and Fluxes for welding, brazing, soldering, tinning and cutting.

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The literature illustrates typical applications—in foundries for removing pads and gouging defects in stainless and alloy steel castings, and by fabricators for cutting out corroded sections in pressure vessels and back gouging welds. Other photographs show applications for maintenance in industrial and construction fields. Complete operating instructions are also covered in pictures and text.

Flexible Tubing Line

A complete line of flexible tubing, in diameters from less than 1 to 30 in., is described in a new 4-page bulletin available from Flexible Tubing Corp., Guilford, Conn. The ducting, made of fabric-supported rubber or plastic wound on helical spring wire coils, is lightweight, highly retractable, noncollapsible, nonkinkable, flexible enough to allow up to 180-deg turns and requires no installation skill. Uses of the tubing include portable and semipermanent ventilation service, fume removal, dust collection and materials handling, in original equipment designs and maintenance applications. Catalog C2-3 also includes information on tubing for special applications, and on fittings.

Standard for Transformer-Type Arc-Welding Machines

The new October 1952 edition of the Standard for Transformer-Type Arc-Welding Machines supersedes the edition of September 1937.

The new edition of the Standard has been prepared as the result of careful study by the Underwriters' Laboratories' staff; it has been reviewed by and discussed quite extensively with manufacturers of listed welding machines of the transformer type, and with others known to have an interest in the requirements which this Standard contains; and it has been approved by the Laboratories' Electrical Council, which is comprised largely of inspection authorities throughout the United States.

The new edition will become effective on July 1, 1953; except that machines listed by the Laboratories prior to that date on the basis of a half-hour or a 1-hr continuous-operation test will continue to be so listed until July 1, 1954. The Laboratories' review of currently listed welding machines will be made prior to July 1st of next year.

Hobart Arc-Welding News

Volume IX, No. 3, *Hobart Arc Welding News*, a 20-page booklet of interesting photographs and articles on welding from all over North America, is now available. Copies are mailed free of charge to anyone interested in arc welding.

Many of the articles feature time- and money-saving applications. To get your copy, write the Hobart Brothers Co., Troy, Ohio.

Special Steels

A comprehensive handbook detailing its line of top quality tool and special purpose steels is now available from The Pyramid Steel Co., P. O. Box 1226, Cleveland 3, Ohio.

Illustrated and described are steels for complete maintenance and repair purposes. Included are nontempering steel, pneumatic and shock steel, turned-ground and polished shafting steel, various types of heat-treated alloy steels for those "problem jobs," brake die steel, oil hardening die steel and abrasive resisting plates. Additional pages cover types for rebuilding worn crawler grousers and for filler bar applications.

The handbook also includes welding instructions for maintenance applications, tables and useful information on steel hardness numbers plus approximate hardness conversion numbers for steel. Still another section gives users technical data such as decimal equivalents.

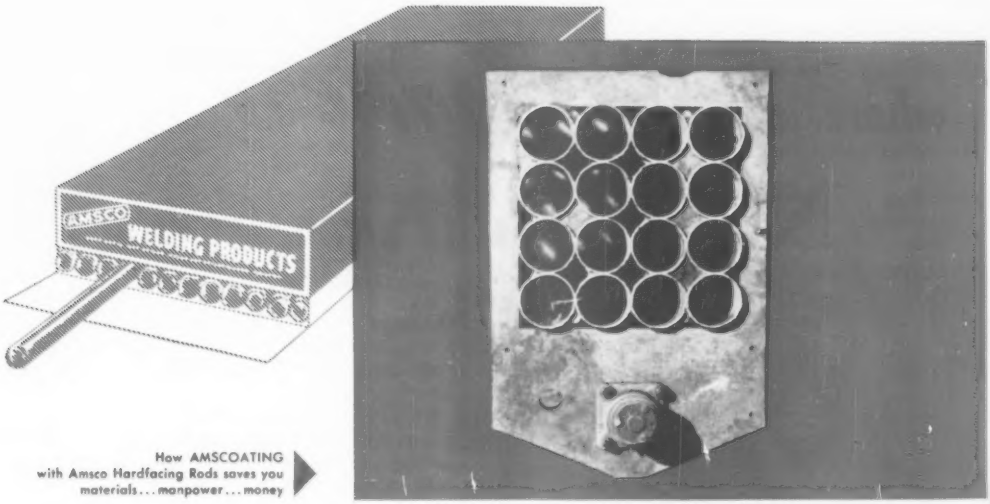
The colorful, 22-page literature contains detailed specifications, applications, operating instructions and other vital technical information which can be used by plant operating personnel.

Welded Highway Bridge Design

Welded Highway Bridge Design is a new, 240-page book edited by Professor James G. Clark of the University of Illinois, and presenting significant designs and details of welded bridges submitted in the Lincoln Foundation's Award Program for welded through highway bridges. Publication date is Nov. 1, 1952. Chapters discuss structural types, floor systems, new sections, connections and details, quantities and costs. Over 100 original drawings clearly reproduced. Edited by J. G. Clark, University of Illinois. Stiff board, cloth-bound covering, gold embossed. Price $2.00 in the USA., $2.50 elsewhere; postage paid by publisher, James F. Lincoln Arc Welding Foundation, Cleveland 17, Ohio.

The authors of the designs include consulting engineers, bridge engineers of state highway departments, structural engineers with governmental departments, designers of steel fabricating companies, structural engineers of research organizations and professors of structural engineering. These professionally well-known men are active designers in 17 different countries, including 20 different states in the United States. The designs incorporate their author's high caliber, experiences and recognized ability for creative thinking.

(Continued on page 1186)



How AMSCOATING with Amsco Hardfacing Rods saves you materials... manpower... money

Amscoating these briquette machine dies increases die life... reduces need for repairs

How to AMSCOAT briquette machine dies

This procedure sheet gives complete details... the Amsco electrode to use and method of application. Also lists recommendations for other iron and steel applications. Free copy on request.

A big reduction in costs of die maintenance has been made by Amscoating with Amsco Economy Hardface "C," according to the welding superintendent of a southern plant manufacturing ferro silicon briquettes.

By using 100-110 amperes reverse polarity and depositing one pass of Economy Hardface "C" on edges of both sides of die, the need for repairs was substantially reduced.

Amsco Economy Hardface "C" is preferred because of its better service results and because of its better handling during application than types of rods used previously.

Long service life and fewer replacements through the use of Amscoating on applications means big savings. If you have equipment that is subject to wear, the possible savings to you are too big to be overlooked!

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Other Plants: New Castle, Del., Denver, Oakland, Cal., Los Angeles, St. Louis. In Canada: Joliette Steel Division, Joliette, Que.
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where jobs are *Toughest*

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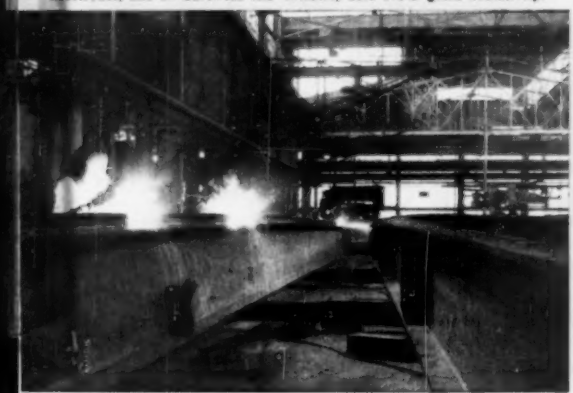
Lee C. Moore Corporation, Tulsa, one of the country's foremost builders of oil well drilling and production masts and structures, is a user of Sureweld Electrodes, has 18 Sureweld AC Welders, uses NCG gases exclusively.

The tough jobs—those that demand unusually high performance under exacting conditions, that require *extra* ability-to-perform—are the ones that put equipment to a real test.

It's on jobs like these that NCG welding and cutting equipment proves most convincingly that it's *built better* to do *better work*, because that extra ability-to-perform is so evident.

Why not find out what NCG can do for you? The literature offered you on the opposite page explains the superiority of NCG's welding, brazing and cutting equipment. Use the convenient coupon to check the literature you wish to receive.



ABOVE: Sureweld Electrodes in use at Harris Structural Steel Company, New Market, New Jersey, one of the nation's largest fabricators of steel bridges and buildings.

ABOVE: An NCG Type R cutting machine is used for shape cutting in the diversified maintenance operations of Esso Standard Oil Company's big Baton Rouge refinery. For production cutting, as many as eight torches can be mounted on the Type R.

BELOW: Eleven Sureweld Spot Welders are in use at the Dunnington Mfg. Co., Indianapolis. Operators make an average of 4,850 welds daily without fatigue. Savings over previous soldering operations are estimated at over $150 per month.

BELOW: At the Neville Island plant of Shanango-Penn Mold Co., Pittsburgh, Torchweld cutting torches are used to remove "staples" from 6-ton ingot molds. Shanango-Penn has used NCG products for 15 years.

USE THE COUPON BELOW FOR HELPFUL NCG® PRODUCT LITERATURE

1 SUREWELD A-C WELDERS

Twelve page catalog fully explains and illustrates the performance and construction features of Sureweld A-C welders. Includes both general utility shop models and heavy duty industrial models.

2 CUTTING AND WELDING APPARATUS

Catalogs on the Torchweld and Rego lines of oxyacetylene welding, brazing and cutting torches, tips, regulators, outfits and other apparatus.

3 SUREWELD "GASARC" INERT-ARC WELDERS

Brochure describes Sureweld "Gasarc" Welders and auxiliary equipment for inert-arc welding. Five models available ranging from 150 to 500 amp. ratings.

4 PORTABLE CUTTING MACHINE

Illustrated catalog describing the famous Cut-O-Matic—NCG's portable automatic motor-driven flame-cutting machine. Cuts straight lines, circles, arcs and bevels. Weighs less than 50 pounds.

5 SUREWELD PORTABLE D-C WELDER

Complete information on the popular Sureweld gas-engine driven D-C Welder which combines ample welding capacity with one-man portability. For field welding, maintenance and repair.

6 POWDER-CUTTING EQUIPMENT

Newly-issued brochure describes NCG's new Ferrojet equipment for cutting stainless and other "hard-to-cut" alloy steels. Also new flame-washing torch for removing riser pads on alloy castings.

7 NCG CYLINDER MANIFOLDS

Sixteen-page catalog describes and illustrates all sizes of NCG cylinder manifolds for economical piped distribution of oxygen, acetylene, hydrogen and nitrogen.

ADDITIONAL NCG PRODUCT LITERATURE

8 Sureweld Portable Spot Welder
9 Sureweld Electrodes
10 NCG Brazing & Welding Alloy Rods
11 NCG Multiple-torch Cutting Machines
12 NCG Welding Accessories
13 Sod-R-Braze Air-Acetylene Torch
14 Torchweld Acetylene Generators

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PAGE Welding Electrodes AND Rods

The designs are original designs for an all-welded two-lane through highway bridge with a span of 250 ft. The original drawings of elevations, sections and details are presented along with the author's discussion of his designs. Major features of enough of the program's designs are presented to insure that most of the new, important and useful information developed for the program is included.

The book is divided into chapters covering structural types, floor systems, new sections, connections and details, and quantities and costs. This arrangement facilitates comparison between similar features of different designs. Arches, trusses and other types of designs are represented in many variations. Floor systems included concrete, steel grid and steel plate. New sections suggested are for plates, tees, beams, channels and angles.

Book Review—New German Handbook on Welding

Werkstoff und Schweissung, Vol. I, edited by F. Erdmann-Jesnitzer, 1002 pages, 6½ x 9½ in., Akademie Verlag, Schiffbauerdamm 19, Berlin NW7, 1951, price $38.40 for two volumes.

Material and Welding is the title of this new German handbook, of which the first of two volumes was published recently. The editor has attempted to assemble in these two volumes all that is known about welding, and has come close to accomplishing his objective. He is not content with laconic literature or catalog references, but constructs general explanations and discusses the "why" of welding phenomena.

The handbook is a happy blend of the production and research viewpoints. Each section of the handbook is written by a specialist. The sections range from highly scientific discussions of heat flow and fatigue factors to detailed descriptions of repair work. In general the contributors provide sound frank engineering analysis of welding procedures, diagnosis of defects and research results. The multiple authorship involves some lack of consistency of emphasis, but no single author nowadays can do justice to the broad field of welding.

The editor is director of the Institute of Metallurgy and Materials Testing at Freiberg Mining Academy in the Eastern Zone. He also organized the large new Central Welding Institute in Halle. Work on the handbook was begun in 1945 and involved 56 contributors. Every contribution was reviewed and criticized by as many as ten other collaborators. No less than 8420 letters passed back and forth among the authors.

The first volume deals with ferrous materials, the second with nonferrous

ANOTHER EXAMPLE OF

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SQUARE D's NEW 4-way VALVE for SPOT WELDERS

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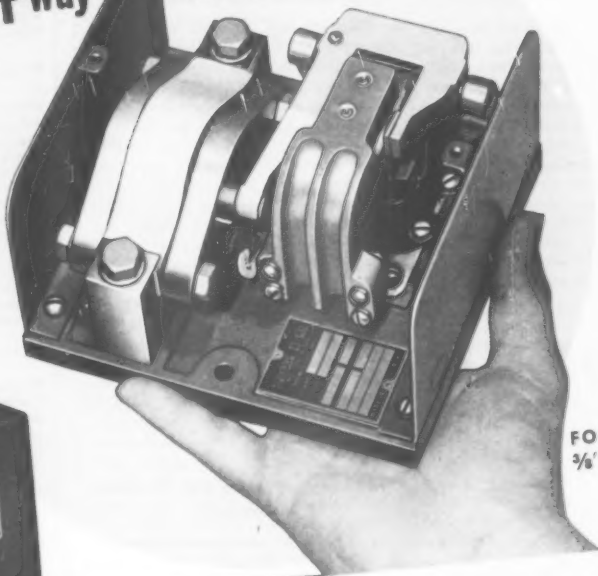

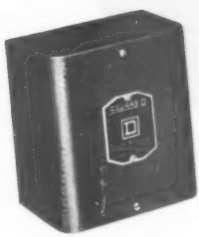

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Fast Operation—Direct-acting magnet...short, rotary motion . . . will operate up to 600 times per minute.

Fast Air Movement—Internal port areas greater than ⅜" pipe area . . . minimum resistance to flow . . . designed to get maximum response speeds from welding guns or stationary machines.

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Write for Bulletin 9043 Type C, Square D Company,
4041 N. Richards Street, Milwaukee 12, Wisconsin

SQUARE D COMPANY

metals and German welding codes. The index is in the second volume. Five outstanding sections in the first volume are (1) "The Theory of Arc Welding," by H. Le Comte and W. Röll; (2) a 50-page section of the "Application of the Similitude Principle to Heat Flow Problems in Welding," by H. Hagen; (3) 80 pages on "Filler Metal for Gas and Arc Welding," by K. L. Zeyen; (4) 60 pages on "Thermit Welding," by W. Ahlert; and (5) 90 pages on the "Shape Aspect in Design of Welded Structures," by A. Erker and A. Thum.

These sections alone would make the handbook a valuable addition to a welding library.

Book review prepared by G. E. Claussen.

Gloves

All-leather heat-resistant gloves, said to be able to "take" temperatures up to 800° F, are described and pictured in a new bulletin offered by the distributor, Mine Safety Appliances Co., Pittsburgh 8, Pa.

The gloves, manufactured by Liberty Dressing Co., Gloversville, N. Y., are said to be pliable, durable, comfortable and economical. They are the development of nearly 10 years of research, the bulletin states. Soft, flexible leather impregnated with insulating and refractory materials is used. In seam construction guards the threads against undue wear. The inner lining, of soft, selected leather, is sewed into finger tips to prevent separation when gloves are removed from perspiring hands. Models featured in the bulletin are 113G for temperatures up to 500° F, 213G with reinforced preshrunk palm up to 800° F, extra-long (13-in.) models, and outseam reversible mitts of similar construction.

Details, including prices, are contained in the bulletin, available without charge or other obligation from Mine Safety Appliances Co., Braddock, Thomas and Meade Sts., Pittsburgh 8, Pa.

Weight-Strength Analysis of Aircraft Structures

Weight-Strength Analysis of Aircraft Structures, By F. R. Shanley, Professor, University of California at Los Angeles and Consultant to The RAND Corp., meets the need for scientifically sound methods of analyzing and predicting the structural weight of aircraft and missiles. Part I discusses methods by which the minimum weight can be determined for any material and conditions of loading. These principles of design for minimum weight are then utilized to derive weight equations for wing and fuselage structures, the subject matter of Part II. Part III includes a number of reports prepared in the course of investigations of new materials and extreme operating conditions.

This book was published by the McGraw-Hill Book Co., Inc., 330 W. 42nd St., New York 36, N. Y. Its price is $8.50 per copy.

Soldering, Brazing and Welding Aluminum

A new folder, just published by All-State Welding Alloys Co., Inc., 249-255 Ferris Ave., White Plains, N. Y., condenses into small envelope-enclosure size complete instructions on how to solder, braze and weld aluminum and aluminum alloys. Covered are 15 All-State Alloys and four All-State Fluxes comprising what the manufacturer heralds as the world's most complete distributor-sold line of materials for joining and sealing aluminum. Sketched briefly, but comprehensively enough to serve as an operating guide, are: precautions and good practices; fluxing information, including how to make flux-coated rods on the job; and step-by-step procedures for soldering, filling, sealing, torch brazing, furnace brazing, torch welding and arc welding. Anyone having to join aluminum to aluminum or aluminum to dissimilar metals such as copper, nichrome, stainless steel and the like should have a copy of this new folder. Requests should be addressed to the company or to any of the 650 All-State distributors throughout the country.

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TITANIUM	
Weldspool 930	
STAINLESS	
Weldspool 304 ELC	Weldbest 307
Weldspool 308	Weldbest 308
Weldspool 309	Weldbest 309
Weldspool 309 Cb	Weldbest 310
Weldspool 310	Weldbest 316
Weldspool 316	Weldbest 330
Weldspool 321	Weldbest 347
Weldspool 347	Weldbest 349
Weldspool 349	
STRAIGHT CHROMIUM STEELS	
Weldspool 405	Weldbest 410
Weldspool 410	Weldbest 430
Weldspool 420	Weldbest 442
Weldspool 430	Weldbest 446
	Weldbest 501
	Weldbest 502
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Weldspool 90000	Weldbest 100
Weldspool 120000	Weldbest 230
	Weldbest 260
HARD SURFACING	
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NON FERROUS ALLOYS	
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Weldspool 610-SiBr	Weldbest 620C PBr
Weldspool 620-PBr	Weldbest 730 CuNi
Weldspool 630-AlBr	Weldbest 760 Cast Iron
Weldspool 730-CuNi	
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Send for Technical Literature

WELDWIRE COMPANY, INC.
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abstracts of
CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25c from the Commissioner of Patents, Washington, D. C.

2,611,848—"Radiant Heat Welding Equipment"—Oliver Ross Smith, Fort Worth, Tex.

Welding equipment for use in welding pipes is disclosed in this patent, and the equipment includes a cylindrical casing adapted to be positioned about the abutted ends of two sections of pipe supported in end-wise abutted relation. The casing has semi-circular elements forming side walls for supporting the casing on the pipe sections, and semi-cylindrical elements forming a cylindrical wall. Other means connect the cylindrical wall to the side wall for circumferential movement about the pipe sections. Substantially semi-circular electrodes are carried by the cylindrical wall in radially spaced relation to the abutted ends.

2,612,394—"Welding Stud"—Ted Nelson, San Leandro, Calif., assignor to Gregory Industries, Inc., a corporation of Michigan.

Nelson's patented welding stud has an aperture in the welding end thereof and a slug of welding flux is expanded against the sides of the aperture to fix the slug in the aperture.

2,612,580—"Welding Method and Apparatus"—Raymond C. Andersen, Lombard, Ill., assignor to Chicago Metal Hose Corp., Maywood, Ill., a corporation of Illinois.

In this patent, a method of welding a metal tube from strip material by making a longitudinal seam in the strip material is disclosed. The method comprises arranging an elongated electrode in the tube, providing an elongated filament electrode and engaging the outer surface of the tube to be welded against the filament electrode at a welding station. The tube and elongated electrode are moved through the welding station while passing current between the electrodes and while continuously pressing the filament electrode against the tube. Thereafter the filament electrode is removed from contact with the tube.

2,612,581—"Welding Rod"—Harold Robinson, Somerville, N. J., assignor to Air Reduction Co., Inc., New York, N. Y., a corporation of New York.

A composite welding rod comprising at least one element of aluminum and at least one element of copper is covered in this patent. The several elements are uniformly and closely associated throughout the length of the weld rod and the proportion of aluminum present is at least 7.5% but is not over 20% by weight.

2,612,582—"Combined Arc Welding Electrode Holder and High Frequency Generator"—William Arnold Appleton, Wembley, England, assignor to Arc Manufacturing Co. Ltd., London, England.

A special electrode holder for use in arc welding is disclosed in this patent and the holder includes a conducting end piece which has means for securing an electrode thereto. A high-voltage, high-frequency generator is positioned within an insulating container in the electrode holder and provides high-frequency electrical energy for flow to the conducting end piece for use in welding operations.

2,612,583—"Arc Welding Rod and Method of Producing Same"—Arthur A. Bernard, Chicago, Ill.

The welding rod of this patent is made from rimmed steel reduced in cross-sectional area by cold working in stages. The reduction in each stage is not greater than 20% and the final cross-sectional area is not substantially more than 25% of the initial cross-sectional area. The steel is maintained below the temperature detrimental with respect to the arc-influencing effect of the cold working.

2,612,584—"Inert Gas Welding Torch"—John P. Morrissey, East Orange, N. J., assignor to General Electric Co., a corporation of New York.

The patented welding torch has a ceramic nozzle for directing a gas flow about the arc and it has an electrode extending through the nozzle from the torch. A heat-resistant tube having an internal diameter larger than that of the electrode is secured to the ceramic nozzle and has a plurality of holes in the wall thereof. These holes extend from the inner to the outer wall of the tube in the direction of flow of an inert gas provided to the torch. Thus a flow of inert gas is directed along the electrode toward the work to be welded to cool the tube and prevent oxidation of the tube.

2,613,015—"Welded Tank Construction"—Leonard Mathew Keating, Milwaukee, Wis., assignor to A. O. Smith Corp., Milwaukee, Wis., a corporation of New York.

This patent relates to a hot water tank that has a generally cylindrical shell and an end head. The tank has a ceramic enamel lining with a nonmetallic corrosion-resistant gasket disposed between the shell and the end head at overlapping sections thereof. An electric resistance weld joins the shell and head ends together around the tank.

2,613,303—"Pipe Welding Machine"—Charles A. Babbitt, South Gate, Calif., assignor to Cal-Metal Corp., Torrance, Calif., a corporation of California.

A pipe forming and welding machine is covered in this patent and includes a plurality of pairs of cooperating rollers for gripping and feeding a pipe blank which has a longitudinally extending slit therein. Certain of these rollers engage the pipe blank in special relation to the marginal edges of the blank, and the first driven roller has a greater peripheral speed than the succeeding rollers whereby a blank fed to the machine in spaced relation to a preceding blank will catch up with and abut against such preceding blank. The machine has automatic means for welding a seam in the blank to produce a pipe.

2,613,304—"Welding Rod or Wire"—Rene D. Colinet, Philadelphia, Pa., assignor to LaSoudure Electrique Autogene S.A., Brussels, Belgium, a Belgian company.

Colinet's welding rod or wire has an aggregation of solid fluxing particles dispersed throughout a continuous solid mass of a matrix metal of substantially higher melting point than the fluxing particles. The solid fluxing particles are not fused in contact with the molten matrix metal. The metal has a structure indicative of fast-cooling casting followed by mechanical compression in the plastic stage.

2,613,305—"Welding Device"—Robert W. Clack, Lafayette, Calif., assignor to the United States of America as represented by the United States Atomic Energy Commission.

This welding device has an evacuated

chamber with means for inserting a pair of metallic parts to be welded in the chamber. High-frequency voltage is connected between the parts and voltage control means are connected to the voltage source for setting the voltage between the parts to produce multipaction. Other means in the device force the parts into contact after heating of the parts by multipaction therebetween.

2,613,353—"Welding Helmet with Air Pressure Responsive Shiftable Eye Protective Lens"—Walter A. Schnitzler, Emerald, Wis.

This patented shield unit relates to a welder's head shield which has a shiftable antiglare lens for positioning in front of the welder's eyes. Such lens is controlled by an inflatable bag, the inflation of which is controlled through a mouthpiece that permits the wearer of the shield to blow air through the mouthpiece and into the bag for changing the position of the lens.

2,614,146—"Seam Welder Head"—Edwin A. Mallett and Melvin M. Seeloff, Warren, Ohio, assignors to The Taylor-Winfield Corp., Warren, Ohio, a corporation of Ohio.

This patent relates to apparatus for rotatably supporting and conducting welding current to a rotary electrode of an electric resistance welding machine. This machine has a projecting rigid support with a spindle journaled therein and extending outwardly of opposite sides of the support. A hollow conductor encases the free end of the support and has aligned bores in opposite walls thereof for the reception of portions of the spindle so that the conductor is floatingly mounted in the spindle for axial movement thereon. A rotary electrode can be positioned on one end of the spindle and the positioning means for the electrode are adapted to provide a predetermined pressure engagement between an adapter ring on the spindle and a current-conductive ring secured to the conductor and brought into pressure contact with the adapter ring by movement of the conductor axially along the spindle.

LIST OF NEW MEMBERS

Effective October 1, 1952

ANTHONY WAYNE

Alley, Richard Lee (B)
Alley, Robert (B)
Keep, Winfred A. (B)
Terry, Lloyd E. (B)

BOSTON

Brimblecom, Frank C. (B)
Huggins, Robert A. (D)
Weidig, Edwin F. (B)
Wood, George H. (B)

CHATTANOOGA

La Barbera, Joe (C)

CHICAGO

Adgate, F. N. (B)
Collins, Thomas S. (B)
Dekker, Roger W. (B)
Jacobs, W. H. (B)
Kellan, Edwin F. (C)
Kolb, Ray M. (C)

CINCINNATI

Filer, Roy C. (C)
Fox, Charles E. (C)
Furnish, Gilbert (C)
Walz, Howard (C)
Wilson, Charles D. (C)

CLEVELAND

Adair, David C. (B)
Besemer, John S. (C)
Fenner, James E. (C)
Shaw, Harold V. (B)
Smith, William D. (D)

COLORADO

Peltz, Peter (B)

DETROIT

Batuk, Claude A. (C)
Lachner, Robert (C)
Olsen, Edward F. (B)
Shearer, Thomas W., Jr. (B)

EAST TEXAS

Emerson, Thomas A. (D)
Turner, Harold H. (D)

HARTFORD

Colella, Freeman T. (C)
French, Robert L. (B)

HOUSTON

Monroe, Dewey J. (C)

INDIANA

Blanck, Walter H., Sr. (B)
Blythe, Ervin W. (B)
Murray, Clifford R. (B)

IOWA-ILLINOIS

Bovin, Alex (B)
Bowe, William J. (C)
Briehl, Neil J. (B)
Carnaghi, John (B)
Castle, Clarence (C)
Crelly, Jack W. (C)
DeDecker, Peter (B)
Devinney, Andrew G. (C)
Dodge, Vaughn A. (B)
Fairley, William H. (B)
Faust, M. W. (B)
Findlay, Joseph T. (C)
First, Harry V. (C)
Fruit, Glen O. (B)
Geisler, Warren (B)
Gustafson, A. R. (B)
Hawkins, Herbert L. (B)
Henry, Ray (B)
Ingalls, Alpheus A. (B)
Ingram, Orville W. (B)
Jacobs, Albert H. (B)
Kauss, George J., Jr. (B)
Larrance, Harry (B)
Lindsay, Robert T. (B)
Lingafelter, John W. (C)
Long, Virgil K. (B)
Martin, Henry I. (B)
McVey, James F. (B)
Milam, Willard L. (C)
Miller, Clyde S. (B)
Morgan, Stanley B. (B)
Nauschultz, William C. (C)
Oneth, Ernie (B)
Pahl, Charles H. (B)
Paustian, George (C)
Plunkett, Stewart V. (B)
Rathjen, Edwin H. (B)
Reck, George W. (B)
Roeh, Henry (B)
Roemer, Peter (C)
Sanders, Lee L. (B)
Schaecher, Joseph H. (C)
Schellhous, Dean (B)
Shewry, Charles W. (B)
Shugars, John W. (B)
Smith, S. J. (B)
Smithers, John A. (B)
Snyder, Carl V. (B)
Sulser, Erwin (C)
Swinburn, Tom A. (B)
Tracy, Wendell (C)
Williams, Don E., Sr. (C)
Willwerth, Charles W. (B)

KANSAS CITY

Hoyt, Rebert L. (B)
Rode, Harry J. (C)
Wehner, Joe (C)

LONG BEACH

Fogleman, John W. (B)
Maither, Archie F. (B)
Manson, Charles J. (C)
Ronk, Edmond W. (C)
Starkey, Gerald Wayne (C)

LOS ANGELES

Harding, Wallace C. (C)
Henderson, Charles R. (B)
Neufeld, Arthur W. (C)
Redfern, Harry J. (B)
Snyder, Paul J. (C)
Thompson, A. M. (B)
Williams, Arthur J. (B)

LOUISVILLE

Widman, Stanley (B)

MARYLAND

Mack, Howard G. (C)
Walker, Robert (C)

MILWAUKEE

Lang, Don (C)
Linnehan, John J. (B)

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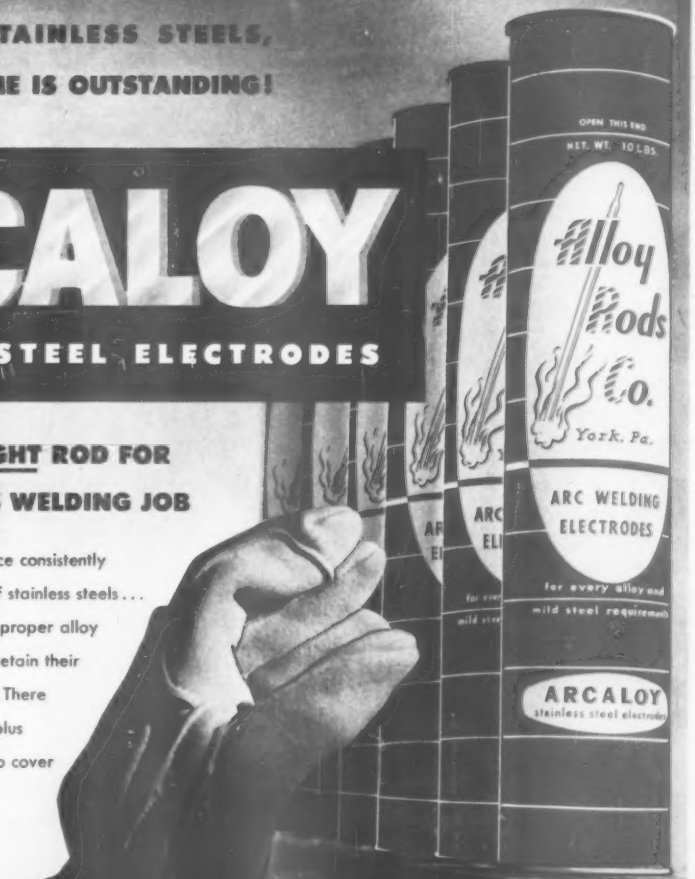
STAINLESS STEEL ELECTRODES



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ARCALOY electrodes produce consistently better welds on all grades of stainless steels . . . weld deposits containing the proper alloy balance. As a result, metals retain their corrosion-resistance qualities. There are 23 regular analyses . . . plus several special analyses . . . to cover every stainless application.

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AC-DC - for *smoother* welding of chrome-nickel steels with all types of AC or DC welding equipment.

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Stroessenreuther, Roy (B)

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Hackett, William P. (B)
Kea, Kirk L., Jr. (B)
Mercurio, Salvatore P., Jr. (B)
Schendorf, John (B)
Spinelli, Carmen F. (C)
Waugh, Harold C. (C)

NEW YORK

Auer, Emil (C)
Kiel, Walter A. (C)
Liggera, Carmelo (B)
Munroe, Ethelmer S. (B)
Obrzut, John J. (C)
Seip, Henry F. (B)
Walsh, John J. (B)
Zylinsky, Charles W. (B)

NIAGARA FRONTIER

Moore, John R. (B)
Saxton, Robert J., Jr. (B)

NORTHERN NEW YORK

Moran, John W. (C)

NORTHWESTERN PENNSYLVANIA

Bittler, Wallace F. (C)
Kaminski, Edwin (C)
Patterson, Archie A. (C)

OKLAHOMA CITY

Wedel, Paul (C)

PASCAGOULA

Anderson, Allen O. (B)
Bebler, Stanley M. (C)
Boyette, Herbert S. (C)
Chapple, Joe M. (C)
Davis, Andrew G. (C)
Davis, E. C. (C)
Forde, Robert K. (B)
Jones, M. H. (C)
Kelly, J. E. (C)
Miller, Joseph K. (B)
Moncrief, E. L. (C)
Peattie, D. G. (B)
Tillman, Troy B. (C)
Tonsmeire, Henry B. (B)
Wages, F. A. (C)

PEORIA

Hill, Edward E. (C)
Johns, Harold D. (C)
Parker, Raymond W. (C)

PHILADELPHIA

Aarts, Cornelius (C)
English, Lawrence P. (C)
Gage, Carl C. (C)
Johnson, Wallace C. (B)
Maladra, Anthony (C)
Masterson, Henry Hart (B)
Ritter, R. Kenneth (C)
Taylor, Rex A. (C)
Vencius, Albert J. (C)
Wheaton, Willard E., Jr. (B)

PITTSBURGH

Falgione, Nicholas F. (C)
Taylor, Dana M. (B)

PORTLAND

Gavigan, Lee T. (B)
Lewis, Robert S. (B)

PUGET SOUND

Snider, William E. (C)

ROCHESTER

Cimino, Vincent J. (C)
Fischer, James H. (C)
Moore, William H. (C)
Perlock, James (C)

SAGINAW VALLEY

Adams, Donald C., Jr. (C)
Haring, Jerry W. (C)
Huntoon, Roy (C)
Wesolowski, Joseph (C)

ST. LOUIS

Smith, F. Raymond (C)

SALT LAKE CITY

Bradley, R. S. (B)

SAN FRANCISCO

Buckwalter, J. L. (B)
Erichsen, Wallace J. (C)
Raymond, W. V. (B)
Van Tuye, W. L. (C)
Waller, Colonel J. (B)

SUSQUEHANNA VALLEY

Hoffman, Henry L. (C)
Williams, William J. (C).

SYRACUSE

LeFevre, H. J. (C)

TULSA

Herod, Robert G. (C)

WESTERN MASSACHUSETTS

Ferenczi, Russell Howard (B)

WESTERN MICHIGAN

Turpin, A. T. (B)

WICHITA

Woerner, I. A. (B)

WORCESTER

Handy, Roy E., Jr. (B)
Jamnbeck, Allan A. (B)
Jamnbeck, Tygo A. (B)

YORK-CENTRAL PENNSYLVANIA

Koons, Robert E. (C)

NOT IN SECTIONS

Anderson, R. J. (B)
Burton, Raymond E. (B)
Cartlidge, Willard A. (C)
Hall, Hubert Tivnann (B)
Pryce, A. A. (B)
Sack, Malcolm Dean (B)

Members reclassified

During the month of October

CHICAGO

Bloomfield, Carl W. (D to C)

DETROIT

Robinson, James M. (C to B)

IOWA-ILLINOIS

Palmer, W. Leroy (B to A)

LOS ANGELES

Burnside, True (C to B)

LOUISVILLE

Spotts, Francis E. (C to B)

MILWAUKEE

Myers, Robert D. (D to C)

NIAGARA FRONTIER

Jennings, C. H. (B to F)

PHILADELPHIA

Davis, Charles H., Jr. (C to B)

SAGINAW VALLEY

Hoffmeyer, Paul (C to B)

WESTERN MASSACHUSETTS

Trombley, William (C to B)

SPECIAL PACKAGE OFFER—AWS CODES AND STANDARDS

The SOCIETY has made available a "package" arrangement whereby any individual, library or company may purchase a complete set of codes, standards, specifications and books on welding published by the SOCIETY and covering the subjects of Fundamentals of Welding; Training, Inspection and Control; Processes; and Industrial Applications. Complete set of these publications are listed under general classifications A, B, C and D of the 1952 Order Form which will be furnished upon request. In addition to these existing publications, two binders are supplied for containing same and *a 10-year service is provided whereby the subscriber automatically receives new or revised standards issued during the next 10 years.*

This complete "package" of present publications, binders and 10-year service is available to companies who support the SOCIETY through Supporting Company membership at a price of $25; members enrolled in the Member (B) Grade classification as well as libraries (public, school or other endowed) at a price of $35; members enrolled in the Associate Member (C) Grade classification at a price of $39 and to nonmembers of the SOCIETY at a price of $50.

In the instance where the 10-year service alone is desired (no binders or existing standards furnished), the price to Supporting Companies is $10; "B" members and libraries $17; "C" member $19 and nonmembers $27.

Your order should be accompanied by either check, money order or company purchase order and mailed to AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y.

PERSONNEL

R. D. Wasserman Elected to Young Presidents' Organization

The membership committee of the Young Presidents' Organization has advised of the unanimous election of R. D. Wasserman, President of Eutectic Welding Alloys Corp., Flushing, N. Y., to membership in that organization.

The Young Presidents' Organization is an interesting, unique and exclusive group of young Americans. To be a member, a man must have become president, before reaching the age of 39, of an industrial or service corporation doing a minimum of $1,000,000 gross business annually plus other qualifications.

Objectives of the organization are threefold, namely: personal friendships, exchange of ideas and to take aggressive action in support of those principles that will further the development of individual incentive.

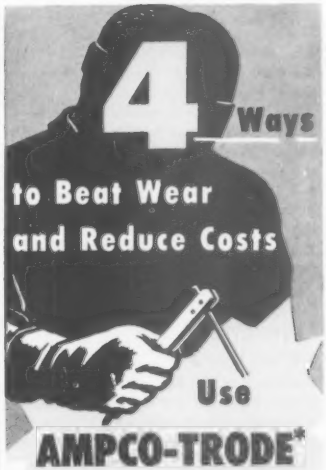
RWMA Prize Winner

Warren F. Savage, co-author of the paper winning first prize from a university source, was born in Harvard, Mass., Mar. 10, 1922. He received the degrees of Bachelor of Chemical Engineering in 1942, and Master of Metallurgical Engineering in 1948 from Rensselaer Polytechnic Institute. After serving as Metallurgical Engineer for Adirondack Foundries and Steel in Watervliet, N. Y., from 1942 to 1944, he returned to Rensselaer, where he was employed con-

secutively as a Research Fellow, an Instructor and is at present employed as an Assistant Professor in the Department of Metallurgical Engineering. He serves in the capacity of Consultant for several concerns, including the Oak Ridge National Laboratory at Oak Ridge, Tenn. He has published several papers in the field of arc and resistance welding. He is a member of Phi Lambda Upsilon, Sigma Xi and is at present chairman of the Eastern New York Chapter of the American Society for Metals.

F. L. Blodgett Advanced

Advancement of F. L. Blodgett from



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Hard-grade Electrodes

The fight against costly wear is a never-ending struggle. But a great many are winning the battle with these four Ampco-Trode hard-surfacing aluminum bronze electrodes.

1. **Ampco-Trode 160** — for repair or fabrication overlays on bearing surfaces subject to shock, impact, and wear. Brinell hardness . . . 160 average.
2. **Ampco-Trode 200** — for repairs and maintenance or fabrication overlays on machine parts subject to unusually severe service. Brinell hardness . . . 200 average.
3. **Ampco-Trode 250** — for overlays on bearing or wearing surfaces working against hardened steel under pressure. Brinell hardness . . . 250 average.
4. **Ampco-Trode 300** — for overlays on forming and drawing dies and other metal-to-metal wear surfaces. Eliminates scratching and galling of the work. Brinell hardness . . . 300 average.

These are four low-cost ways to beat wear. Keep a supply of these electrodes on hand. Then you can choose the right rod for your needs. For further information, consult your Ampco distributor or write us.

AMPCO W-110

AMPCO METAL, INC.
MILWAUKEE 46, WISCONSIN
West Coast Plant: Burbank, California

IT'S PRODUCTION-WISE TO AMPCO-IZE!

*Reg. U. S. Pat. Off.

the post of Manager of the Hard-Facing Division to Assistant General Sales Manager of the Alloy Rods Co. was announced by E. R. Walsh III, General Sales Manager of the company. The appointment was effective October 1st.

Mr. Blodgett has been associated with various phases of the welding industry for the past 15 years. In his new post, he will be closely associated with national distributor activities, and the merchandising and advertising programs of Alloy Rods. His headquarters will be at the factory in York, Pa.

Boss Made Regional Manager

Robert Boss of 1716 Ridge Road East, Rochester, N. Y., has been appointed Regional Manager for All-State Welding Alloys Co., Inc., to cover all of the state of Ohio and the western portions of New York and Pennsylvania. Announcement of this appointment was made by Bent Laune, President, at the Company's offices, White Plains, N.Y.

Mr. Boss will be responsible for sales and service to the users and distributors of All-State alloys and fluxes in his area. He will be the contact man for All-State's metallurgical resources and experimental laboratory which provide technical assistance for the solution of all manner of problems having to do with the welding, brazing, soldering, tinning and cutting of metals for maintenance, reclamation, construction and production of metal parts and equipment.

Having been in the welding field since 1936 except for three years in the Air Force in Alaska, South America, Africa and England, Mr. Boss is particularly well equipped for his new responsibilities. He has served the field as a highly skilled welder, in an engineering capacity and as a supply salesman. Previous to joining the All-State organization, he was a representative for Jackson Welding Supply Co. in Rochester. He is a member of the Rochester Section of the AMERICAN WELDING SOCIETY and has been on their executive committee for the past two years.

TWECO CABLE CONNECTORS

—"SOL-CON" (solder type)
—"MEC-CON" (mechanical)

Quick-detachable connectors. Add on cable. Take off cable. Attach holder whip cables. Split male plug permits adjusting spring tension for electrical contact. Two sizes, Sol-Con; three sizes, Mec-Con for #6 through 4/0 cable.

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SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Resistance Welding

Chicago, Ill.—The regular monthly meeting of the *Chicago* Section was held on October 10th at the Peoples Gas Light & Coke Auditorium. Dinner at Burke's Grill & Restaurant preceded the technical meeting.

Dean L. Knight, AWS, Chief Engineer, National Electric Welding Machine Co., was the speaker. His subject was "Measuring for Good Resistance Welding." Mr. Knight spoke on new machine developments, new design, problems of instrumentation and how they are approached. Mr. Knight acts as liaison between sales, engineering and the customer. He has the viewpoint of user as well as manufacturer.

A film showing the local AWS Golf Tournament was also shown and enjoyed by all.

Pipe Line Movie

Cincinnati, Ohio.—Messrs. Rudolf Kraus AWS and William Maddox AWS presented an extremely interesting colored movie showing many of the problems and events incident to the construction of a 1300-mile pipe line by welding. Since welding made such pipe lines possible, this picture was of interest to the entire fraternity. Slides collected by Mr. Kraus were also shown during the discussion period. These showed a large number of actual construction practices which were fabricated by welding.

The above took place at the regular monthly meeting of the *Cincinnati* Section held at the Engineering Society headquarters on October 28th.

Low Hydrogen Electrodes

Cleveland, Ohio.—The *Cleveland* Section opened fall activities on October 8th with an excellent turnout of 150 persons for dinner and the following technical session. Chairman Frank Flocke AWS opened the meeting after dinner outlining the program for the second Wednesday of the month meetings to be held through May 1953. He introduced the Board of Directors and Committee Chairman. The services of Past Chairman John Austin were recognized and acknowledged with thanks.

Following these introductions and a brief talk by Membership Chairman John Rogos AWS the meeting was turned over to Howard Schnell, Coffee Speaker Chairman, to introduce the speaker. Cliff Lewis, a six-year veteran of the Cleveland professional football team, the Browns, presented his personal reactions to the team members and their prospects for the year. He followed his comments with a movie of the game highlights of the 1951 season.

The technical session got underway at 8:30 with Lew Gilbert AWS, Chairman of the Program Committee, introducing the speaker of the evening, D. L. Mathias AWS, Research Department, Arcrods Corp. His subject was the development of low hydrogen electrodes and an explanation of the correct procedures for their use.

Mr. Mathias clearly defined the advantages of this type of electrode, stating that failure to achieve maximum performance from them was usually because of a lack of understanding of the requirements for their application.

He emphasized the importance of maintaining low moisture content in the coating by proper storage and handling; warned against slag entrapment in narrow grooves; illustrated proper joint design to account for the lower arc voltage and less wetting action of the electrodes; pointed out the necessity for striking the arc ahead of the start of the joint to let a crucible form in the coating at the tip of the electrode; demonstrated the need for holding a short arc, slower travel speed and straight motion when welding vertical. He closed by stating that these electrodes are not a cure-all for a universal electrode but have very definite advantages for welding steels normally considered difficult to weld. He re-emphasized that because of the heavy coating used and the design of the coating, special attention is needed to procedures.

The meeting closed with a lively question period in which Mr. Mathias generously shared his knowledge of electrodes with the audience.

Tentative Dallas Program

Dallas, Texas.—At a meeting of the Executive Committee of the *Dallas* Section held on Tuesday, October 14th, the following temporary line-up of speakers and programs for the remainder of the year was given:

December 16th—Dinner meeting followed by plant visit at Fritz W. Glitsch & Sons.

January or February—D. C. Smith has been invited to talk on "Proper Selection of Welding Rods." Definite time and date will be announced soon.

March—Ralph Triman of the Reed Engineering Co.

April—Charles H. Jennings AWS of the Westinghouse Corp.

May—An attempt is being made to get a speaker on maintenance welding.

Plant Visit

Dayton, Ohio.—An excellent plant visit to the Moraine Products Division, GMC, manufacturers of hydraulic brake equipment, bearings and powdered metallurgy was made on October 14th by 38 members of the *Dayton* Section.

Submerged Melt Welding

Denver, Colo.—D. C. Ransone of the Linde Air Products Co., a division of Union Carbide & Carbon Corp., was the guest speaker at the October 21st dinner meeting of the *Colorado* Section held at the Shirley-Savoy Hotel. Mr. Ransone's talk on "Submerged Melt Welding" covered a brief history of the process, a thorough discussion of the present state of mechanical development and an enlightening preview of future prospects.

In place of the usual coffee speaker the AWS film "Applied Welding Engineering" was shown. The membership enjoyed the film and was particularly impressed with the amount and scope of printed information which the AWS makes available to its members. Everyone seemed to feel that these pamphlets contain a large fund of very practical information which they had been overlooking in their endeavor to solve many of their daily welding problems.

Plant Tour

Des Moines, Iowa.—The October meeting of the *Iowa* Section held on the 23rd was a plant visitation to the Solar Aircraft Co. of Des Moines to view, in operation, one of the largest collections of production welding equipment in the United States. Over 175 resistance welders and 60 fusion welding stations are in constant service mass producing jet-engine components. Approximately 110 members and guests were present representing a majority of the leading metalworking plants and welding distributors in the Central Iowa region.

Following a cafeteria style dinner, a very interesting tour was made of the big five-story Grand Avenue plant where high-temperature components of several jet engines are manufactured. The tour was conducted to include nearly all operations on the six productive floors. Some of the major operations observed include shearing, forming, heat treating, machining, assembling and, of particular interest, welding.

At some stage or another, practically every jet engine component is fabricated by one of the many welding processes available. Representative of the various processes are spot and seam welding, projection welding, flash welding, manual and automatic inert-gas-arc welding, metallic-arc welding, sigma welding, automatic submerged arc welding, oxy-acetylene welding and induction brazing. In addition, many types of Solar-made welding equipment were demonstrated in operation, of particular interest were the automatic inert-gas-arc seam welders and tackers, spot tackers and various welding fixtures which were designed and built in the Plant Engineering Department. Also of interest to those making the tour was the variety of inspection processes, the plants chemical and physical laboratories, the method of quality control in assuring acceptable welds, the welding laboratory and the welding school where fusion welders are trained and certified to weld, by various processes, the many different types of stainless and other alloy steels employed.

The members and guests were pleased with the tour and expressed their appreciation for the manner in which their guides, members of the Company's Welding Engineering Department, showed them through the plant.

Jet Engine Alloys

Detroit, Mich.—"Resistance and Arc Welding of Jet Engine Alloys" an interesting and timely subject was presented by Julius Heuschkel [M] one of the outstanding men in the jet alloy welding field, through the courtesy of the Westinghouse Electric Corp., leader in the manufacture of jet engines. His talk, accompanied with illustrated slides, was presented at the October 10th dinner meeting of the *Detroit* Section held at the Engineering Society of Detroit.

The developments of the various alloys and their properties were reviewed by the speaker, who pointed out their field of application, welding problems encountered and their limitations. The importance of the subject was proved by the many questions which the audience asked in the discussion which followed.

Mr. Heuschkel is a graduate of Montana State College and was associated with the U. S. Navy on welding research for seven years. For eight years he held a position of Manager, Welding Section, Carnegie Illinois Steel Co. For the past eight years he has been Manager, Welding Section, Westinghouse Research Laboratory, Pittsburgh, Pa.

Following Mr. Heuschkel's talk, the sound color movie "Arc Welding Stainless Steel" was shown through the courtesy of Allegheny Ludlum Steel Corp.'s Detroit Office. This film illustrates the types of bead, manipulation of the arc and precautions to be observed.

Tool and Die Welding

Erie, Pa.—Pat S. Doyen [M] was the speaker at the technical session of the *Northwestern Pa.* Section held on October 14th. Mr. Doyen gave a clear-cut presentation of the subject "Tool and Die Welding," which was of interest to the engineer, sales and operator.

Maintenance Welding

Fort Wayne, Ind.—T. B. Jefferson, Editor of *Welding Engineer* and Vice-President of District 5 of the AWS, was the dinner guest speaker at the October 17th meeting of the *Anthony Wayne* Section held at the Howard Johnson Restaurant. Mr. Jefferson's talk "Maintenance Welding—A National Need" emphasized the importance of maintenance welding in the industrial job shop and structural fields. Slides were shown to illustrate a portion of the talk. The slides showed pumps, impellers, pump cases and other equipment used on the Montana dam project in the process of reclaiming for continued use. The speaker described many of the processes used on a diversified maintenance program. The meeting included a lively question period where members presented problems to the speaker.

T. B. Jefferson is a graduate mechanical engineer from the University of Kansas and for the past 20 years has been closely allied with welding. Since 1940 he has been Editor of *Welding Engineer* and *Welding Encyclopedia*.

Film on Tool Steel Welding

Grand Rapids, Mich.—A self-explanatory 35 mm film with recording was shown by Art Hedges [M] of the Miller Welding Supply Co., Grand Rapids, at the dinner meeting of the *Western Michigan* Section held on October 27th at the Elks Club Cafeteria. The film was loaned by the Welding Equipment and Supply Co. of Detroit and demonstrated the use of their "Eureka" tool steel welding rods.

After-dinner feature was a freedom forum by the Junior Chamber of Commerce of Ionia, Mich.

Sigma Welding

Hartford, Conn.—Approximately 63 members and guests were present at the October 9th dinner meeting of the *Hartford* Section held at the Rockledge Country Club in West Hartford. A semitechnical talk was given by J. M. Tippett of the Linde Air Products Co. who spoke on the subject "Sigma Welding."

Manufacture of Electrodes

Houston, Tex.—The second meeting of the *Houston* Section for the year was held at the Ben Milam on Oct. 29, 1952. The meeting was preceded by a keg, a movie and a dinner. The movie shown, "Fishing in New Zealand," was made by Alfred C. Glassell, Jr., a Houston oil man who makes fishing and movies his hobby. He presented the film to the Houston Museum of Natural History and is was through their courtesy that we used it.

R. K. Lee [M] Vice-President of Alloy Rods Co., York, Pa., was the speaker for the evening. His talk was preceded by a movie on the manufacture of electrodes at the Alloy Rods plant. His talk illustrated with slides, covered the welding of stainless steels for the chemical and petroleum industry, a subject of great interest to the users in this vast chemical and petroleum area.

Eighty-nine members and guests were present for this meeting.

Schedule of Meetings

Indianapolis, Ind.—The following is a schedule of meetings for the remainder of the season of the *Indiana* Section:

Nov. 21st, 6:30 P.M.—"Design for Silver Alloy Brazing." Speaker: R. J. Metzler, Handy & Harman, New York, N.Y., includes film. Dinner Meeting. Buckley's Restaurant, Cumberland, Ind.

Dec. 19th—Christmas Party—Family Night. Severin Hotel, Indianapolis, Ind.

Jan. 23rd, 6:30 P.M.—"Developments in Resistance Welding." Speaker: R. T. Gillette, Precision Welder & Flexopress Corp., Cincinnati, Ohio. Dinner Meeting, Buckley's Restaurant, Cumberland, Ind.

Feb. 27th, 6:00 P.M.—Exhibits and Demonstrations of Latest Developments in Welding, W. O. Mullendore, Air Reduction Sales, Indianapolis, Ind.; Culley Heffernon, Linde Air Products Co., Indianapolis, Ind. Dinner Meeting. Reuas Engineering Co., Indianapolis, Ind.

Mar. 27th, 6:30 P.M.—Double Feature. "New Engineering Materials, Titanium, and Metal Ceramics." Speaker: Lee Busch, P. R. Mallory & Co., Inc., Indianapolis, Ind. "Origin, Development, and Uses of Synthetic Gems." Speaker: F. O. Jagels, Jr., Linde Air Products Co., Chicago, Ill. Dinner Meeting, Family Night, Athenaeum, Indianapolis, Ind. Exhibits.

Apr. 24th, 4:00 P.M.—Intersectional Quiz Team Contest, Antlers Hotel, Indianapolis, Ind. Social hour and dinner prior to contest.

May 22, 6:30 P.M.—Plant Tour of CAA Facilities, Weir-Cook Airport, Indianapolis, Ind. Dinner Meeting, prior to tour. Dining place to be announced. Annual election of officers.

Sigma Welding

Kansas City, Mo.—In recognition of Oil Progress Week the *Kansas City* Section at their October 9th, dinner meeting had as a coffee speaker Dr. Marlin N. Volz, Dean of the School of Law, University of Kansas City. Dr. Volz gave a very informative talk entitled "World Oil in Peace." He outlined the theory on the formation of an oil field and methods used in detecting and developing the field. He then discussed the political importance of the location of the world's various major oil fields. Following Dr. Volz's talk, Mr. McGarry of the Standard Oil Co., presented a film on "Oil for Tomorrow."

Speaker at the technical meeting was J. E. Dato of the Linde Air Products Co., and District 6 Vice-President. Mr. Dato gave an excellent talk on the subject "Application of Sigma Welding. He used both slides and movies to illustrate his talk. The movies were ultra slow motion showing the rod-to-base metal transfer under various operating conditions.

Automatic Hardfacing

Long Beach, Calif.—The October 17th dinner meeting of the *Long Beach* Section was held at Millers Restaurant. Henry S. Warren, Factory Representative of the Stoody Co., gave an excellent presentation of the subject "Automatic Hardfacing." Black and white slides were used to illustrate Mr. Warren's talk. Mr. Warren is in charge of training hardfacing specialists. For seven years he has worked in welding field engineering. He has been with the Stoody Co. since February 1950.

Welding Aircraft Wing Tip Tanks

Los Angeles, Calif.—The highly successful meeting of the "Aircraft and Rocketry Welding Panel" was held on October 10th of the *Los Angeles* Section at the San Diego Country Club. Ninety-six members and guests were present at the dinner and meeting which followed. George Brolaski AWS, Chairman, introduced K. W. Maynard, Rohr Production Engineer. Mr. Maynard pointed out that the successful production of the P2-V Boeing Wing Tip Tank was due to the team work of the welding and tooling engineers.

Jack Maloney AWS, Production Welding Foreman of Rohr Aircraft Co., showed a movie on the P2-V Wing Tip Tank. Narration was by Mr. Maloney.

The movie demonstrated the various sequence operations of making the various wing Tip Tank components, specifically the weldment assemblies.

The Wing Tip Tank components which are joined together by resistance and fusion welding processes are made from 61S-W aluminum alloy. The tank is "tear drop" in shape. It consists, mainly, of two half shells which are formed by stretch forming.

The climax of the total operation of making one of these tanks is the fusion welding of the two longitudinal seams. These seams are automatically welded by the inert-gas-shielded nonconsumable electrode process. A very unique hold-down fixture holds the two shells in position during welding with the aid of mandrel connectors. The hold-down feet are actuated by an air pressurized fire hose to a contour template. The welding head, voltage regulated, is propelled by a specially designed graham drive travel unit. The welding head is fixed to a vertical shaft which moves up and down. The vertical shaft is intergral with a contour cam follower.

All welds are nondestructively inspected by the dye penetrant method.

Many of the internal fuel tank subassemblies are also joined by fusion welding. In some cases a combination inert-gas-shielded arc and oxy-hydrogen procedure is utilized. Mr. Maloney pointed out that when proper fit-up was hard to maintain on butt-type joints the oxy-hydrogen welding method was employed.

Chairman H. Clemens, AWS extended his appreciation for the excellent meeting and the manner in which it was conducted.

F. Stevenson AWS, Aircraft and Rocketry Welding Panel Chairman, also extended his thanks for the wonderful attendance and the panel's mutual appreciation for the eventful evening.

Headquarters Memo Note: The success that has attended the institution and activities of the AWS Los Angeles Section Aircraft and Rocketry Welding Panel has been phenomenal to say the least. The Aircraft Industry in Southern California, the AWS Los Angeles Section and the Panel Officers are to be greatly commended upon their achievement. This Panel actively satisfies a long-felt need for a vehicle which would bring together in meeting and discussion the executives, engineers and production specialists of the Aircraft Industry. An example is provided for all other AWS Sections, as many have specialized industrial groups which could be served in comparable manner.—J. G. MAGRATH, *National Secretary*

Silver Alloy Brazing

Louisville, Ky.—Roger J. Metzler, AWS, Handy & Harman, New York, gave an excellent extemporaneous nontechnical talk on "Design for Silver Alloy Brazing" at the dinner meeting of the *Louisville* Section held on October 28th at Kapfhammer's Party House.

Rockets

New York, N. Y.—The first regular monthly dinner meeting of the season was held on September 16th by the *New York* Section at Schwartz's Restaurant. The subject of the technical meeting, "Rockets—their Past, Present and Future," was presented by Frank Coss, Project Engineer, Reaction Motors, Inc., Dover, N. J. Mr. Coss spoke extemporaneously on guided missiles, and rocket planes and how they obtain their power through the thrust of rocket motors. The history, development and future possibilities of all types of rockets were discussed in popular style. In conjunction with the presentation a movie showing recent developments in rocket motors including scenes of German V-2 rockets in flight was also of great interest.

Frank Coss is in charge of development of 6000-lb thrust airplane rocket engines. He is a graduate of the University of California. He was formerly connected with the Wright Aeronautical Corp. Since 1946 he has been in the rocket field with Reaction Motors, Inc.

Welded Buildings

New York, N. Y.—H. W. Lawson AWS Consulting Engineer, read an excellent paper on the subject "Welding Building Structures" at the October 14th dinner meeting of the *New York* Section held at Schwartz's Restaurant. Mr. Lawson's technical paper was illustrated with slides.

Program of New York Meetings

New York, N. Y.—The following is a program of meetings for the *New York* Section for the remainder of the season:

December 9th—"Radiographic Standards for Arc-Welded Joints," Alexander Gobus AWS, Head, Nondestructive Testing, Sam Tour & Co.

January 13th—"Heavy Weldment Fabrication," R. W. Bennett AWS, American Locomotive Co.

February 17th—"Welding in Ship Repair," Sidney Swan AWS, Principal Surveyor, New York District, American Bureau of shipping.

March 10th—"Battle of the Sections," Four Neighboring AWS Sections Match Wits on Welding Problems.

April 14th—"Developments in Welding Low-Alloy Steels," D. L. Mathias AWS, Aircrads Corp.

May—Annual Dinner and Entertainment.

Time: Social—6:00 P.M.
Dinner—6:30 P.M.
Meeting—7:30 P.M.

Place: Schwartz's Restaurant, 54 Broad St., New York, N. Y.

Heat in Welding

Oklahoma City, Okla.—Dr. Robert T. Howard AWS, Bendix Aviation Corp., Kansas City, Mo., was the speaker

at the technical session of the September 17th meeting of the *Oklahoma City* Section held in the Biltmore Hotel. The talk on "The Nature of Heat in Welding" was of a technical nature but was presented in such a manner that several local shops requested copies for distribution to their supervisors.

Welding Procedures

Pittsburgh, Pa.—The regular monthly dinner meeting of the *Pittsburgh* Section was held on October 15th in the auditorium of the Mellon Institute of Industrial Research. A get-together at dinner in the Hunt Room of the Hotel Webster Hall preceded the meeting. E. R. Seabloom AWS, Supervisor of Field Engineering, Crane Co., Chicago, Ill., opened the meeting with the Crane Co.'s latest industrial sound movie entitled "Life Lines of Industry" which deals with the control of fluid flow and the manufacture of products servicing almost every industry.

Mr. Seabloom's semitechnical address was on "Establishments of Welding Procedures and Qualifications of Operators" and was of general interest to all.

Mr. Seabloom has been affiliated with the Engineering Division of the Crane organization for many years. His activities have included laboratory development work, shop contacts and field construction. He has devoted much time to design metallurgy and welding research as well as practical engineering problems. He is first vice-president and past-chairman of the AWS Chicago Section as well as a member of the Welding Research Council and other engineering societies.

Zirconium and Titanium

Portland, Ore.—Olaf T. Paasche, Assistant Professor, Mechanical Engineering, Oregon State College, and Amil Killin, U. S. Bureau of Mines, Oregon, were the guest speakers at the October 14th dinner meeting of the *Portland* Section held in the Nortonia Hotel. Their extemporaneous talk on "Welding of Zirconium and Titanium" was followed by a lively question-and-answer period.

Inert Gas Welding

Portland, Ore.—E. B. LaVelle AWS, Welding Engineer, Nucleonics Department, General Electric Co., Richland, Wash., spoke on the subject "Application Analysis of Inert-Gas-Shielded Electric Welding" at the September 9th meeting of the *Portland* Section held in the Mirror Room of the Mallory Hotel.

Mr. LaVelle has been actively engaged in the welding industry in the northwest for the past 10 years. At present he is a member of the National Welding Committee for the Atomic Energy Commission and is engaged in design engineering at the Hanford Works.

Resistance Welding Controls

Saginaw, Mich.—J. W. Brown AWS, Standard Products Section, Square D Co., Milwaukee, Wis., was the guest speaker at the October 9th dinner meeting of the *Saginaw Valley* Section held at the High Life Inn. Mr. Brown's subject was "Resistance Welding Controls." He discussed and demonstrated some of his company's latest synchronous and nonsynchronous control equipment for resistance welding. Actual controls were set up along with an oscilloscope to show the patterns associated with the controls.

Design for Welding

San Francisco, Calif.—Ninety-one members and guests of the *San Francisco* Section were present at the dinner held on October 27th at the Engineers Club. Coffee speaker was Capt. G. W. Underwood, Repair Superintendent of the San Francisco Naval Shipyard, a veteran submariner himself, who discussed the importance that welding has played in the development of the submarine. His subject was "Submarines-Then and Now."

Thomas H. Nicholl AWS, Welding Engineer of the Lincoln Electric Co., spoke on "WELDesign," a coined name given to a new branch of engineering and meaning the proper design of tanks, pressure vessels, structural sections, etc., through the use of welding. Mr. Nicholl discussed the engineer's approach to the correct choice of available materials and their properties which are all too frequently overlooked in this field of designing for welding. Cost criteria and strength rigidity, both in the final product as well as in the reduction of actual welding costs, were covered in Mr. Nicholl's talk which was illustrated with slides and charts.

Program of Northern New York Meetings

Schenectady, N. Y.—The following is a schedule of meetings for the remainder of the season of the *Northern New York* Section:

December 4th—Arc Welding Applications.
February 5th—Brazing Applications.
March 5th—Technical Activities. "Stump the Experts."
April 2nd—"Resistance Welding Applications," P. G. Parks, Solar Aircraft.
May 7th—Annual Ladies Night.

Low-Hydrogen Electrodes

Seattle, Wash.—A forum on low-hydrogen welding, with Howard Long of the General Electric Co., acting as moderator, was presented by the *Puget Sound* Section at their September meeting held on the 11th at the Engineers' Club. Members of the forum were: Howard Long, General Electric Co.; Alex Tallquist, Ederer Engineering; Clark Brown, Pacific Car and Foundry; Robert Miller AWS, Boeing Airplane Co.; William Snider AWS, Webster Welding; James McCormick, Renton Vocational School; Myron Stepath AWS, Arcair Co.

Field Trip

Seattle, Wash.—Herbert A. Schiessl, President, Fentron Steel, Co., showed the welding methods used by his company in the construction of steel and aluminum window sash. Mr. Schiessl's talk was followed by a field trip to the Fentron Steel Co. This took place at the October 9th dinner meeting of the *Puget Sound* Section.

Nondestructive Inspection of Welds

Shelton, Conn.—Dinner meeting of the *Bridgeport* Section was held on October 16th at Rapps Restaurant in Shelton. Robert Insinger, Inspector for the Peabody Engineering Corp., was the coffee speaker. His subject was "Metal Industry in the Netherlands."

Feature speaker of the technical meeting was Roy O. Schievel, Eastern District Manager of the Magnaflux Corp., New York City. His talk "Nondestructive Inspection of Welds" was illustrated by slides and was of excellent quality and was of interest to the engineer, to the man in the production department as well as to the operator.

Plant Visit

South Bend, Ind.—The *Michiana* Section continued its 1952 program with a visit to the Dowagiac, Mich., plant of the Kaiser-Fraser Corp. on October 16th. Following a delicious chicken dinner served by the ladies of the committee of the Dowagiac VFW some 50 members and guests toured this new addition to the aircraft-engine-producing facilities of America. At this stage, relatively little welding is being done but the equipment and production processes made very good "seeing" for the group.

Maintenance Repair

Syracuse, N. Y.—Over 125 members and guests of the *Syracuse* Section at a dinner held on October 8th at the Hotel Onodaga thoroughly enjoyed a talk given by K. M. Spicer AWS, Research Engineer, International Nickel Co. Mr. Spicer's subject was "Maintenance Repair and Casting Salvage." He discussed some fundamental arc welding techniques

for cast iron, stainless steel and special alloys. Mr. Spicer's talk was basic, interesting, well organized and easy to follow, and he is highly recommended to any group interested in welding fundamentals of nickel electrodes.

Technical Highlights Covered by K. M. Spicer

1. All foreign material must be removed from the area that is to be welded or that will be heated by welding when using high nickel or nickel electrodes.

2. If the current density is set for welding clean material, a "repelling" effect will be noted when attempts are made to fuse through an oxide layer, particularly on the vertical member of a horizontal fillet.

3. When welding with nickel or high nickel electrodes, deep penetration and high heat input should be avoided.

4. The use of backing rings should not be used in joints in pipe lines. Crevices cannot be avoided and these may promote corrosion. Also, the notch effect can cause root cracking in some materials.

5. When welds are subjected to high temperature service, avoid corner joints if possible. If corner joints are used, it is very important to secure full joint penetration.

6. A U-type joint is preferred for material heavier than $^3/_8$ in. in thickness.

7. It is desirable to provide a grooved backing or chill bar beneath the joint when welding light gage materials.

8. Since the coefficients of thermal expansion of nickel and high nickel alloys do not differ greatly from mild steel, approximately the same degree of restraint may be used in fixtures.

9. Welding techniques similar to those used in making high quality welds in steel are used with nickel. It is best to position the work whenever possible.

10. When using nickel electrodes use d-c current with reversed polarity.

11. Since nickel does not flow as readily as steel, it may be necessary to weave the electrode slightly. However it is essential that a short arc be maintained.

12. When weld spatter occurs, it is an indication that (*a*) the arc is too long, (*b*) excessive amperage is being used or (*c*) the polarity is incorrect.

13. Complete removal of slag from units intended for high-temperature service is mandatory.

14. When using an inert-gas welding process, air movement in the shop due to fans, welding generators or open windows may disrupt the protective gas around the arc and cause porosity in the weld.

15. A tungsten arc length should not exceed 0.100 in. in length when welding nickel or high nickel alloy.

16. When making overlays, slow down the rate of welding so that the arc is cushioned by the deposited metal. This technique will minimize base metal dilution.

About 45 members and guests enjoyed a Virginia Ham Dinner in the Hiawatha Room of the Onondaga Hotel. A. L. Lipton, an attorney, who has been cited for his work in Civilian Defense, was the coffee speaker. Mr. Lipton spoke of the need for Civilian Defense, how Civilian Defense is organized and how it functions. The talk was a potent capsule of information on a thought-provoking subject.

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(*Continued from p. 1153*)

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Initiation and Propagation of Brittle Fracture in Structural Steels

Tests correlated with ship fracture data indicate that structural steels of the ship plate type develop brittle fracture in the range of service temperatures when loaded in the presence of a sharp cleavage crack defect

by Peter P. Puzak, Earl W. Eschbacher and William S. Pellini

Abstract

Experimental techniques have been evolved to initiate brittle fracture in structural steels from a sharp cleavage crack which synthesizes the fabrication cracks developed in welded structures. The cleavage crack is developed by the cracking of a small bead-on-plate of brittle hard-surfacing weld metal. Loading of the test plates containing such a crack-starter weld is accomplished by the use of explosives or a drop weight. Tests conducted over a range of service temperatures have demonstrated that structural steels of the ship plate type develop brittle fracture in the temperature range of ship fractures. Detailed correlation with ship fracture data indicated that the conditions of crack initiation, propagation and stoppage are reproduced by the tests.

The significance of Charpy V and Keyhole transition criteria for the prediction of conditions of brittle fracture were investigated for various types of structural steels. It is demonstrated that only criteria associated with nearly completely brittle fractures of these specimens are significant to the problem.

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INTRODUCTION

FABRICATION defects of the nature of arc strikes, weld faults, HAZ cracks, etc., have been shown to be responsible for the initiation of a number of ship failures. It is realistic to assume that large welded structures contain many such defects, since it is impossible to assure their complete elimination by either procedure control or inspection. Despite the presence of such defects it is known that the Liberty ships perform satisfactorily at "warm" service temperatures; however, with a drop of 30 to 40° F to "cold" service temperatures numerous catastrophic failures were developed. This problem is not specific to ships, for pressure vessels, storage tanks, bridges, pipe lines, etc., have shown similar tendencies to failure at "cold" temperatures.

The fact that welding may produce sharp cracks which lie dormant until triggered into brittle fractures by unfavorable stress and temperature conditions is only part of the over-all case of catastrophic failure. It is well known that welding may also result in developing regions of low ductility which favor the formation of cracks *during* the course of service. Weldability tests of the Kinzel and related types have shown that, depending on the welding conditions and the intrinsic properties of the steel, either the weld or the HAZ provide the point of initial crack formation. In such tests the weldability of the steel is judged in terms of the

temperature and bend-angle conditions required to develop cracking in the welded specimen as compared to the unwelded material.

The present investigation is not concerned with the question of weldability as related to the comparative ease of development of the initial crack in zones of impaired properties resulting from welding. The question under study relates solely to the performance of the base material in the presence of cleavage cracks, which are considered the most severe type of defect resulting from a variety of causes related to welding. For this study the zone which originates the crack is of importance only to the extent that it be of sufficient brittleness to ensure the development of reproducible cleavage cracks. This was accomplished by developing a small region of extreme brittleness in the test material. Conventional notching techniques are inadequate inasmuch as it is not practical to produce notches of sharpness equivalent to cleavage cracks.

The subject approach divorces the complex problem of weldability from the problem of the fracture characteristics of the base metal. Accordingly, it is possible to rationalize the problem of catastrophic failure of structures to the elements which are specific to the initiation and propagation of brittle fracture in the parent metal. The principal elements that will be considered are as follows: (1) The development of cleavage cracks of extreme acuity in localized regions of low ductility; (2) The initiation of brittle fracture in the parent metal resulting from the presence of the cleavage cracks at the boundary zone of the unaffected parent metal; (3) the availability of elastic strain energy (elastic extension of the structure) required for the propagation of the brittle fracture.

Items (1) and (2) may be synthesized in a test specimen by developing a cleavage crack in a region of high brittleness and loading the specimen to promote the initiation of a brittle fracture in the test material. Item (3) is difficult to reproduce in small specimens. This difficulty results from the fact that at any given stress level the amount of elastic strain energy is proportional to the total length of the specimen or structure. Thus, the energy available for crack propagation in small laboratory specimens may be several orders of magnitude less than that available in a large structure. The importance of this difference is related to the amount of energy required for propagation of brittle fracture, inasmuch as "brittle" fractures vary in the minute amounts of shearing developed near the free surfaces and in the nature of the cleavage. These variations determine the "degree" of brittleness of the fracture and accordingly the amount of elastic strain energy required for its propagation.

In order to maintain elastic stresses in small specimens undergoing stress unloading by brittle cracking, it is essential to provide loading conditions featuring a "follow up" equal to rates of elastic unloading. Since the rate of unloading is proportional to the speed of propagation of the crack, it is necessary to reload at equal rates. If this is not accomplished the available elastic strain energy is quickly consumed and the crack automatically stops.

Fig. 1 Test plate with crack-starter weld

The elements (1 to 3 above) which are required to reproduce the crack initiation and propagation conditions of large structures are considered to be synthesized in the NRL Crack-Starter Bulge Tests. A 14- x 14-in. plate is prepared for test by the deposition of a short bead-on-plate of a brittle hard-surfacing weld, Fig. 1. It was determined by static load tests that the brittle weld cracks when the yield point of the base metal is reached, thus developing the desired conditions of an extremely sharp cleavage crack. The acceptance and propagation of the cleavage crack by the base metal is dependent on its intrinsic properties at the temperature of test. The high-speed load follow-up required to maintain a continuing store of elastic energy necessary to feed propagating cracks is accomplished by the "softness" inherent to loading by the gas-pressure developed during the detonation of an explosive charge offset from the test plate, Fig. 2. In essence, the gas-pressure does not permit stress unloading during cracking.

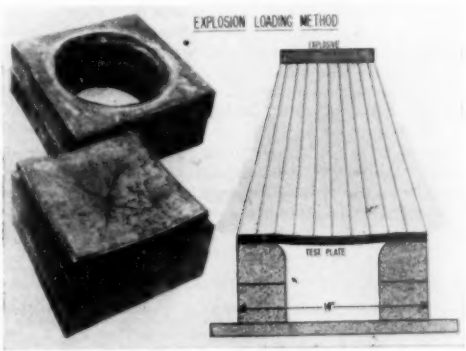

Fig. 2 Details of the NRL crack-starter bulge test

Table 1

Type	Code	Thickness, In.	Year	C, %	Mn, %	Si, %	Al, %[a]	P, %	S, %	Ni, %	Cr, %	Mo, %[a]	Cu, %	Ti, %[a]	V, %[a]
Vanity HTS	1[b]	1	1942-46	0.15	1.24	0.24		0.016	0.024	0.16	0.06	0.010	0.12	0.01	0.01[c]
	2[b, d]	3/4	1942-46	0.15	1.18	0.34		0.025	0.027	0.18	0.15	0.024	0.16	0.01	0.04
	3[b]	1	1942-46	0.15	0.95	0.22		0.017	0.023	0.11	0.15	0.013	0.19	0.01	0.02
	4[b]	1	1942-46	0.16	1.33	0.26	0.014	0.021	0.028	0.05	0.26		0.01	0.05	0.01[c]
	5	1	1942-46	0.14	0.98	0.24		0.019	0.030	0.06	0.03	0.040	0.01	0.02	0.04
	6	1	1942-46	0.16	0.98	0.23		0.016	0.030	0.12	0.09	0.010	0.11	0.02	0.02
	7	1	1942-46	0.14	0.75	0.21		0.023	0.029	0.17	0.17	0.020	0.18	0.01	0.03
	8	1	1942-46	0.12	1.12	0.35		0.025	0.016	0.18	0.15	0.023	0.16	0.01	0.03
Fully killed	9[b, d]	3/4	1942-46	0.12	0.74	0.17		0.029	0.036	0.21	0.13	0.032	0.04		
	10[e]	3/4	1942-46	0.18	0.54	0.25		0.015	0.029	0.18	0.11	0.050	0.20		
	11[e]	3/4	1942-46	0.20	0.54	0.26	0.018	0.012	0.029	0.17	0.24	0.026	0.21		
	12	1	1950-52	0.16	0.62	0.26		0.017	0.040	0.10	0.18		0.11		
	13	1 1/4[f]	1942-46	0.21	0.54	0.21	0.040	0.002	0.029	0.09	0.18		0.11		
	14[d]	3/4	1950-52	0.12	0.59	0.23	0.017	0.010	0.042	0.10	0.11		0.27		
	15	1	1942-46	0.22	0.52	0.21		0.019	0.034	0.03	0.11		0.01		
	16	1 1/4[f]	1942-46	0.24	0.41	0.22		0.007	0.036	0.06	0.08		0.00		
	17	1	1942-46	0.24	0.41	0.18		0.009	0.024	0.05	0.08		0.03		
Semikilled	18	3/4	1942-46	0.16	0.73	0.04		0.011	0.033	0.07	0.06	0.000	0.05		
	19	3/4	1942-46	0.16	0.72	0.05		0.013	0.032	0.07	0.09	0.000	0.07		
	20	1	1942-46	0.17	0.49	0.10		0.007	0.030	0.05	0.11		0.11		
	21[d]	3/4	1950-52	0.16	0.34	0.02		0.004	0.027	0.07	0.13	0.009	0.15		
	22[d]	3/4	1950-52	0.25	0.50	0.05		0.012	0.036	0.04	0.08		0.03		
	23	1	1942-46	0.19	0.49	0.05		0.010	0.017	0.05	0.13		0.02		
	24	1	1942-46	0.23	0.46	0.05		0.009	0.034	0.04	0.08		0.01		
	25	1	1942-46	0.24	0.45	0.05		0.004	0.024	0.04	0.08		0.01		
	26	1	1942-46	0.22	0.46	0.08		0.010	0.030	0.04	0.08		0.01		
	27	1 1/4[f]	1942-46	0.23	0.55	0.07		0.011	0.026	0.04	0.06		0.01		
	28	1	1950-52	0.08	0.32	0.02		0.011	0.037	0.04	0.03		0.03		
Rimmed	29	3/4	1950-52	0.19	0.38	0.01		0.007	0.021	0.07	0.02		0.18		
	30	3/4	1950-52	0.25	0.49	0.01		0.010	0.033	0.11	0.12		0.22		
	31	3/4	1950-52	0.24	0.45	0.02		0.013	0.043	0.12	0.08		0.21		
	32	1	1950-52	0.23	0.40	0.01		0.010	0.040	0.11	0.06		0.33		
	33	3/4	1950-52	0.27	0.41	0.01	0.009	0.037	0.045	0.15	0.14		0.30		
	34	3/4	1942-46	0.25	0.35	0.02		0.008		0.18	0.11	0.030	0.19		
	35	1	1942-46	0.24	0.35	0.03		0.008	0.039	0.16	0.14		0.29		

[a] Spectrographic analysis; % Al and % Mo (—) <0.005. [b] Also tested in normalized condition (N). [c] Less than 0.02% V minimum specified by Navy Department Spec. 48 S5g. [d] Also tested in annealed condition (A). [e] Normalized as received.

The feature of a maintained store of elastic energy is particularly important for studies of the propagation of brittle fractures of high-energy absorption type. Brittle fractures of low-energy absorption type propagate readily even with the small amounts of elastic strain energy available in conventionally loaded small specimens. Accordingly, studies of the latter type of fracture were made using a drop-weight loading technique in addition to the explosion loading method. Appendix A provides details of the specimens and testing techniques.

The general aims of the investigation entailed:

1. Evaluation of various types of structural steels in terms of temperature range of brittle fracture resulting from the presence of extreme flaws.
2. Separation of the initiation and propagation features of brittle fracture.
3. Correlation of fracture characteristics at various temperatures with the transition characteristics of conventional specimens (Charpy V and Keyhole).
4. Correlation of the fracture characteristics of ship steels with the known performance of these steels in ship service.
5. Evaluation of the significance of Schnadt specimen concepts relative to the service performance of welded structures.

MATERIALS AND PLAN OF INVESTIGATION

Table 1 lists the chemical analyses, plate thickness, date of receipt and other pertinent details of the various steels

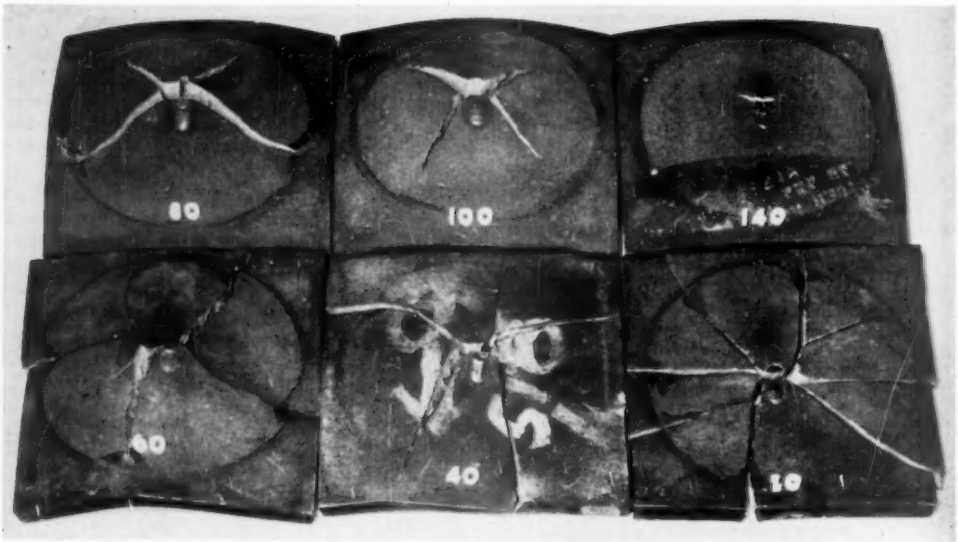

Fig. 3 Typical crack-starter test series for average quality ship plate steel (No. 23). *Figures refer to test temperature ° F*

which were investigated. The date of receipt serves to identify the steels which represent samples of production material used in the construction of Liberty ships. The subject steels represent commercial plate products either of $^3/_4$ or 1-in. (5 exceptions) nominal thickness. Unless otherwise noted all steels were tested in the as-received conditions. Selected steels were retested following normalizing and annealing heat treatments aimed at shifting the notch toughness characteristics to higher or lower temperatures. The original plan of investigation entailed tests of a limited number of steels in terms of a correlation of crack propagation characteristics with the transition characteristics determined by Charpy V and Keyhole specimens. Crack propagation tests were planned for both the longitudinal and transverse directions of rolling. During the course of the investigation it became apparent that the data available were sufficient to establish the effect of orientation so further tests were restricted to longitudinal crack propagation for which the greatest amount of data had been collected at that date.

The original plan to consider both Charpy Keyhole and Charpy V specimen transitions in two orientations was also curtailed as the pattern of orientation and specimen correlation became apparent. In order to accumulate data which could be compared directly with the results of the NBS ship fracture investigation[1] Charpy V specimen (LH type*) transitions were determined for all steels investigated. These modifications permitted extension of the investigation to cover a greater number of steels than was considered feasible with the earlier plan.

* *Note:* The specimen orientation code used in the Penn State Investigation[2] was adopted as shown in Fig. 12.

CRACK-STARTER BULGE TESTS

The characteristics of a typical crack propagation test series are illustrated in Fig. 3. It is observed that at the highest test temperature (140° F) the steel deforms to the limiting bulge size in preference to cracking. Figure 4 illustrates that increasing the energy delivered by the explosive does not result in raising the maximum temperature of crack propagation. At the lowest test temperature (20° F) the steel plate not only develops extensive cracking but also remains perfectly flat. This indicates that the sharp cleavage flaw developed by cracking of the brittle weld at the yield point of the plate results in the immediate initiation of brittle cracks in the plate. The amount of bulging shown at intermediate temperatures (40, 60, 80 and 100° F tests) increases gradually with increasing temperature indicating that increased deformation is required to initiate cracking. The extent of cracking decreases with increased temperature indicating more difficult propagation of the cracks.

It was observed that at temperatures at which cracking is confined to short distances (somewhat longer than shown by the 140° F test) a heavy shear lip approximately 0.050 to 0.080 in. is developed at the plate surface portion of the otherwise brittle fracture. The surface shear lip becomes thinner with decreasing temperature of test, being vanishingly fine at the flat-break temperature, Fig. 5. A similar shear lip which is developed along the sides of Charpy specimens also gradually disappears with decreased temperature of test. It is generally considered that a large fraction of the energy expended in fracturing Charpy specimens in the lower range of the transition curve is related to the develop-

ment of surface shear. The development of shear at the free surfaces of otherwise brittle cracks is related to the essentially biaxial stress features of free surfaces. The disappearance of the shear lip thus indicates the elimination of the last vestiges of ductility related to crack propagation. In this respect it should be noted that plates which fractured through in ship failures occasionally showed distinct shear lips. Measurement of ship fracture plates having shear lips indicated maximum thicknesses of 0.010 to 0.020-in. These measurements were made on representative samples from the extensive NBS stock of ship-plate fractures. Observation along the complete transverse fracture of the Fort Mercer showed a number of plates which fractured with a similar maximum thickness of shear lip. It may be concluded that the amount of elastic strain energy available in ship structures is insufficient to continue the catastrophic propagation of cracks which develop more than 0.010 to 0.020-in. surface shear. Inasmuch as the gas pressure load on the bulge is not relaxed during cracking, a greater energy store is available which is sufficient to propagate (for short distances) bulge cracks having shear lips of 0.050 to 0.080-in. maximum.

The effect of temperature on the extent of cracking in the bulge test is related to the thickness of shear lip developed at the test temperatures. It was observed that cracks which propagated with evident ease through the low-stressed, hold-down regions of the bulge (20, 40, 60° F tests, Fig. 3) did not develop more than 0.010-in. surface shear. The amount of shear was uniform over the entire length of the fracture. Cracks which propagated through the bulge but stopped at the hold-down regions (80° F test) developed surface shear lips

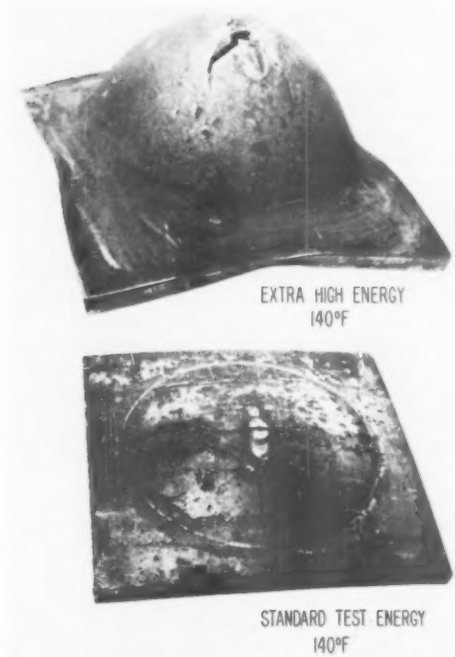

Fig. 4 Behavior at crack refusal temperature

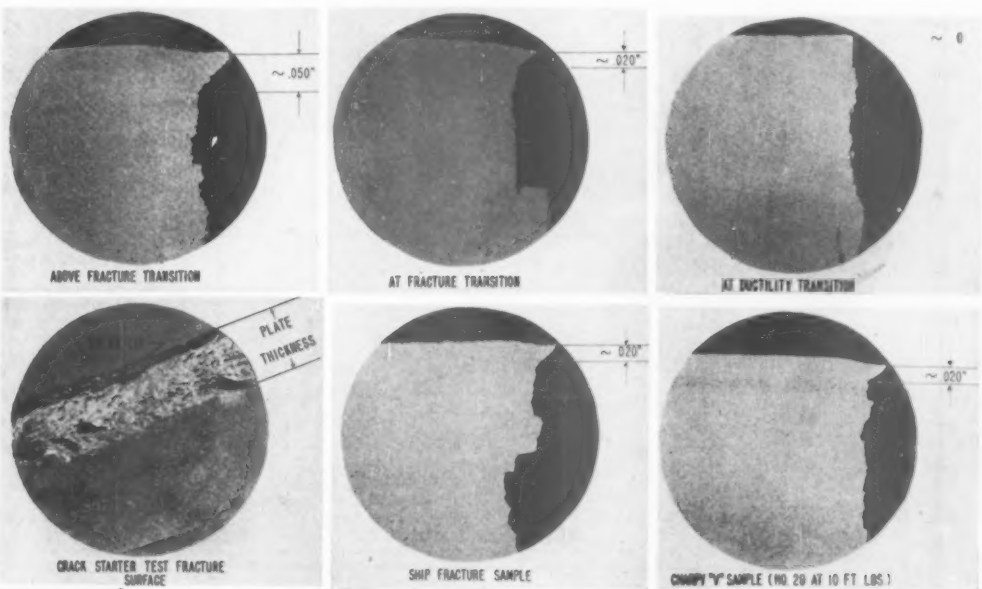

Fig. 5 Examples of shear lips on fracture surfaces

ranging from 0.010 to 0.020 in. The amount of shear developed by this type remained uniform to within $^1/_2$ in. of the point of termination, after which there was a marked increase of shear lip resulting in the slowing down and stopping of the crack. Cracks which showed partial propagation in the bulge area (100° F test) developed shear in excess 0.020 in. The amount of shear developed by this type of crack also increased markedly near the point of termination. As stated previously, very short cracks (140° F test) developed shear lips ranging from 0.050 to 0.080 in.

The controlling effects of the available elastic strain energy and of temperature are also demonstrated by the Robertson test specimen[3] which features the propagation of a crack through a region having a temperature gradient. The crack starts from a notch in the low-temperature portion of the specimen and terminates at a higher temperature position. The temperature at the point of termination is deemed to represent the maximum temperature of crack propagation. Representative fractures of Robertson specimens[3] show that the surface shear lip increases uniformly along the path of fracture due to the thermal gradient condition. Increasing the available strain energy by increasing the stress level of the specimen serves to propagate the crack to higher temperature (higher shear lip) positions; however, a limiting stress is reached above which no further increases are developed. Accordingly, it may be concluded that the exact thickness of shear lip which is effective in preventing the propagation of ship fractures varies, within limits, with the stress level of the region through which the fracture is advancing. For this reason it is not possible to define a *specific* maximum thickness of shear lip which is effective in preventing crack propagation. However, in view of the various considerations outlined above, the critical level of surface shear appears to lie in the range of 0.010 to 0.020 in.

Figure 6 presents a typical surface shear plot illustrating the critical effect of temperature. A smooth curve drawn through the points permits interpolation to determine the temperature range of 0.010 to 0.020 in. surface shear. It will be noted in summary data to be presented that the subject temperature range is relatively narrow; with relatively few exceptions, the range of 0.010 to 0.020 in. shear covered only 10 to 20° F. The critical nature of what may be considered relatively minor temperature changes is illustrated also by the fact that in the majority of the test series an increase of 20° F was sufficient to change the performance from essentially complete fracture in the highly stressed bulge region to essentially complete crack refusal.

Figure 6 also illustrates the general method used to relate the significant fracture characteristics of the bulge test to Charpy V and Keyhole transition curves. Two criteria of bulge fracture performance are represented: (1) Fracture transition—defined as the temperature range of 0.010 to 0.020-in. surface shear, and (2) ductility transition—defined as the temperature at which a brittle fracture is developed in the absence of appreciable prior plastic deformation (flat break).

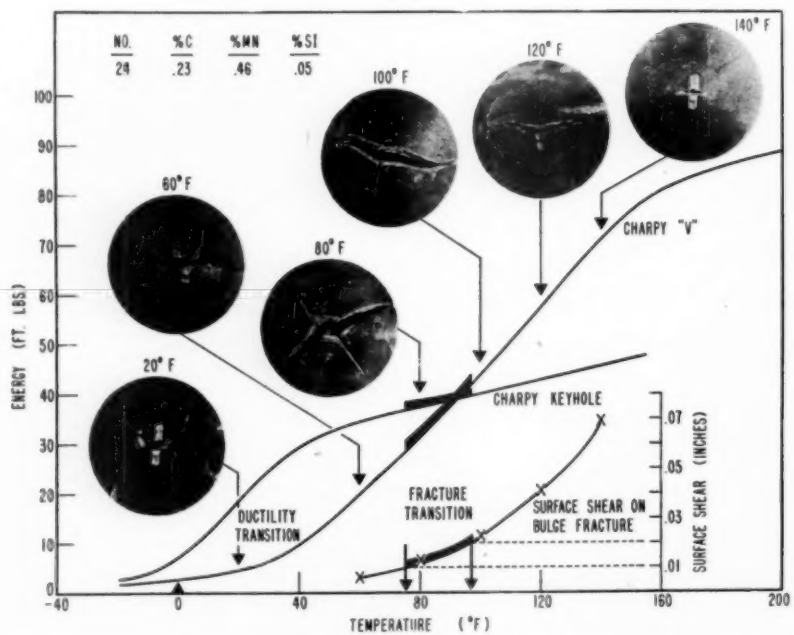

Fig. 6 Surface shear plot of typical crack-starter test series depicting method of determining the fracture transition and of indicating its relation to Charpy curves

RELATION OF FRACTURE AND DUCTILITY TRANSITIONS TO CHARPY V DATA

Figures 7–10 present Charpy V transition curves of the various steels investigated. The heavy-band portion of the curves denotes the temperature range of the bulge fracture transition. All fracture transition curves shown in these figures refer to longitudinal crack propagation. Table 2 provides a comparison of longitudinal and transverse fracture transition data. It is observed that in the majority of cases the temperature ranges show a partial overlap. If the mean tempera-

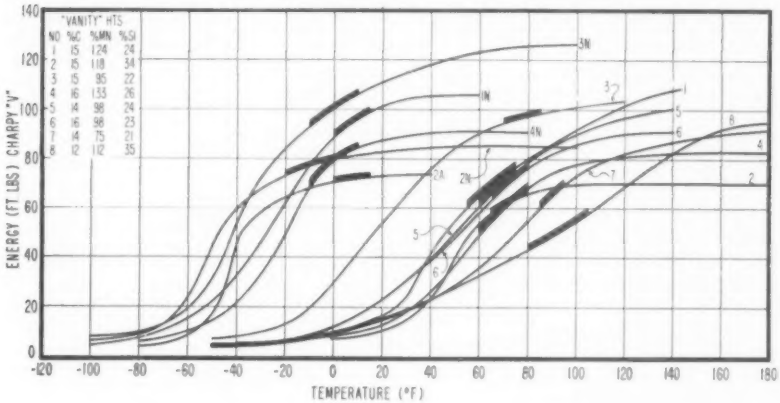

Fig. 7 Temperature range of bulge fracture transitions related to Charpy V transition curves

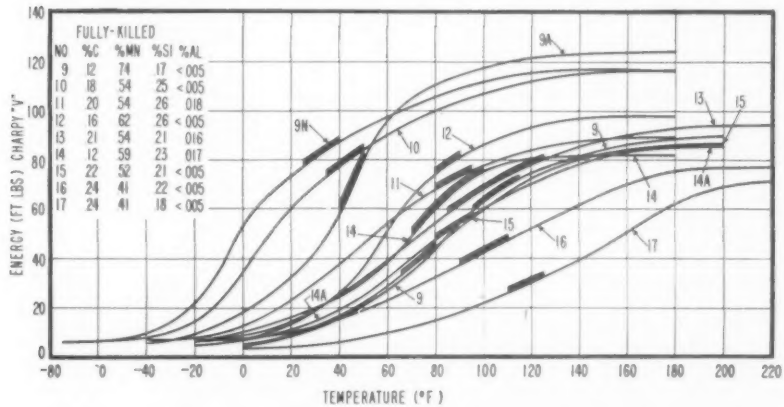

Fig. 8 Temperature range of bulge fracture transitions related to Charpy V transition curves

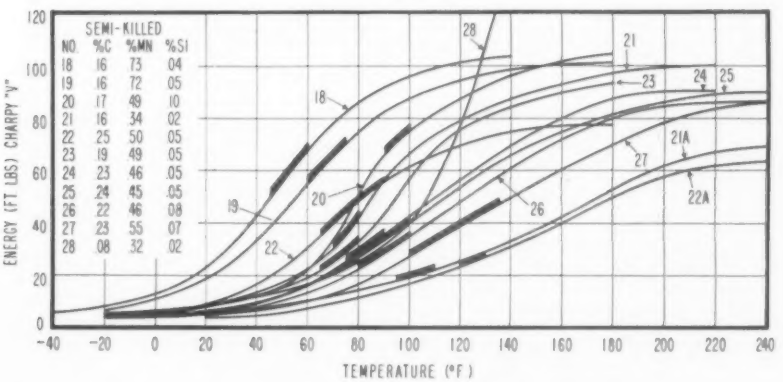

Fig. 9 Temperature range of bulge fracture transitions related to Charpy V transition curves

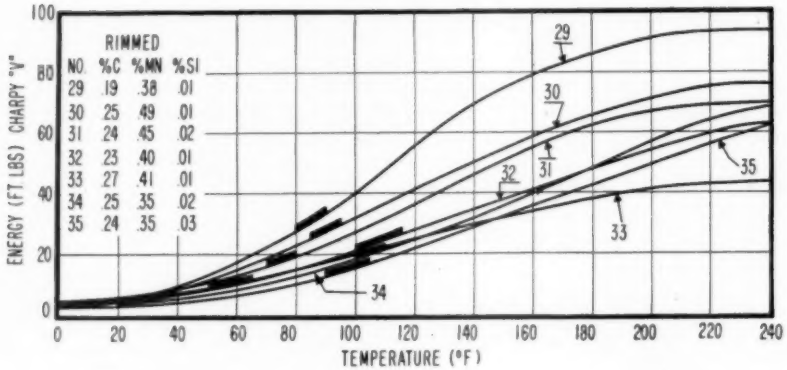

Fig. 10 Temperature range of bulge fracture transitions related to Charpy V transition curves

ture of the range is taken as an index of difference, it is observed that in the majority of cases the transverse crack propagation range is approximately 10 to 15° F lower than the longitudinal crack propagation range. If differences of 10° F are considered to be of questionable significance, the majority of steels show remarkably little directional effects in relation to brittle crack propagation. Figure 11 illustrates the similarity of fracture characteristics in the two directions which was typical of comparison series.

It should be noted that Charpy specimens also indicate that directional effects are relatively unimportant in the temperature range which entails the elimination of last traces of ductile behavior. Figure 12 illustrates the general effect on transition curves of specimen and notch orientation relative to the direction of rolling. It is noted that orientation effects are most pronounced at temperatures involving ductile behavior; as the amount

Table 2—Comparison of Fracture Transition Data[a] for Longitudinal and Transverse Crack Propagation

Steel type	*Code*	*Longitudinal, ° F*	*Transverse, ° F*	*Difference in mean temp., T − L, ° F*
Vanity HTS	1	55–75	35–70	−12
	4	60–80	45–70	−12
	4N	−10–+10	−10–+10	0
	5	60–75	25–35	−38
	6	65–80	60–80	− 3
	7	55–75	35–70	−12
Fully killed	10	35–50	20–30	−18
	12	80–90	55–80	− 8
	14	70–100	45–55	−35
Semikilled	18	45–60	55–70	+10
	19	60–75	60–65	− 5
	20	90–100	70–90	−15
	23	65–80	55–80	− 5
	24	75–90	60–80	−13
	26	80–100	75–90	− 8
Rimmed	31	70–80	65–75	− 5
	35	90–105	85–95	− 8

[a] Temperature range of 0.010–0.020-in. surface shear.

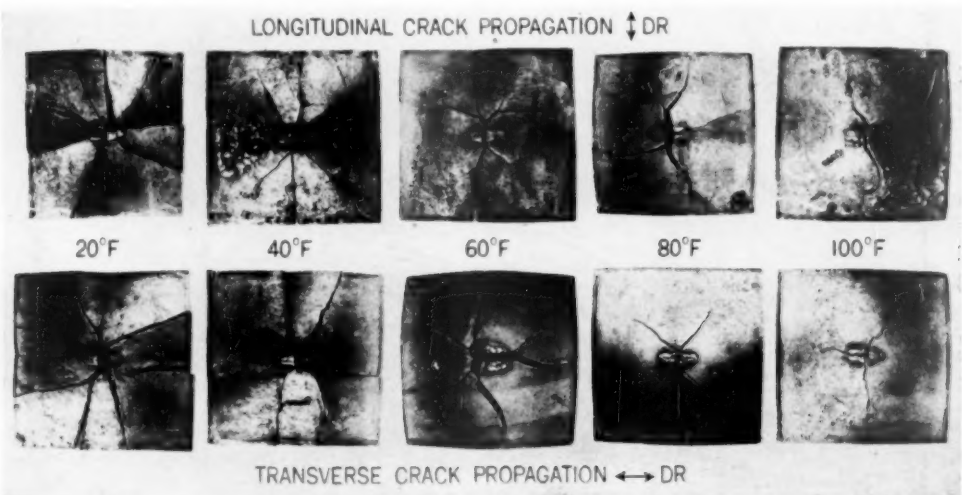

Fig. 11 Example of similarity of fracture characteristics of longitudinal and transverse crack-starter test series (Steel No. 1)

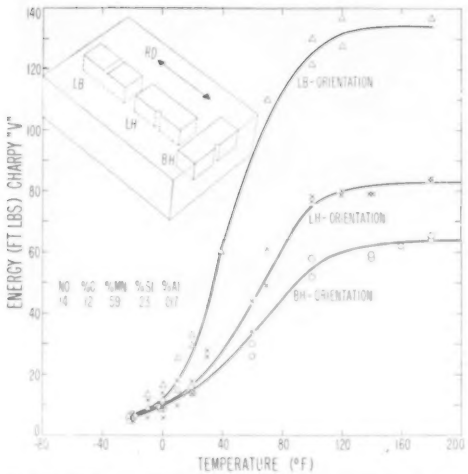

Fig. 12 Effect on Charpy V transition curves of notch and specimen orientation relative to rolling direction

of brittle fracture (percent "grain") increases with falling temperature, orientation differences are gradually eliminated so that in the range of 5 to 10 ft-lb the curves essentially overlap. Comparison of transverse fracture (*LH*) and longitudinal fracture (*BH*) Charpy V transition curves of 30 steels of the present investigation showed no exceptions to this behavior.

The 15 ft-lb transition temperature of the LH specimen in most cases was 10 to 15° F lower than that of the BH specimen. Inasmuch as the Charpy V 5 to 10 ft-lb level represents essentially complete cleavage fracture while the 15 ft-lb level represents a small amount of shear in the fracture, it is concluded that small directionality effects shown by the 15 ft-lb criterion are related to differences in energy absorbed in the shear portion of the fracture. This conclusion leads to the prediction that fracture transitions of the bulge (0.010 to 0.020-in. surface shear) should be slightly sensitive to orientation as was observed, and also that ductility transitions (surface shear nil) should show no orientation effects, which was found to be the case as will be shown in the following sections.

The various Charpy V curves which are presented show that the value of energy absorbed at full shear (energy level of upper shelf) is not significant to crack starting or crack propagation while the relative temperature positions of the toe portion of the curves are highly significant. The following general observations may be made:

1. The bulge fracture transition temperature range does not bear a fixed relationship to Charpy V *energy values* developed in the same temperature range. In general, a rough relationship is observed within classes of steels separated as to deoxidation practice. In the case of rimmed and semikilled steels the fracture transitions occur at temperatures which are related to the lower portion of the Charpy V transition range. Fully killed types develop fracture transitions related to the upper portion of the transition range.

2. The fracture transitions generally shift to lower temperatures with a shift of the toe of the Charpy V transition curves to lower temperatures. This shift is fairly regular within a group but cannot be relied on when different types of steels are compared, unless the Charpy transition shift is of considerable magnitude.

The temperature at which the test plate develops a "flat break" was considered of special significance in that it represents the complete elimination of ability to develop deformation in the presence of a sharp cleavage crack. The term ductility transition was applied to this temperature in recognition of this behavior. It was noted that a "flat break" was developed only for test temperatures which corresponded approximately to the Charpy V 10 ft-lb temperature (20 separate test series indicated a range of 5 to 14 ft-lb, with an average of 8 ft-lb). The extreme brittleness developed at "flat-break" temperatures, as the result of the near absence of surface shear lip, produced extensive break-up of the plates with consequent damage on impact with the die base. In many cases it was thus difficult to evaluate the extent of flatness. For other reasons, mainly of expediency, it was decided to attempt an alternate method of determining the ductility transition. The near-absence of surface shear lip suggested that at the flat-break temperature the amount of elastic strain energy required to support the propagation of fracture would be sufficiently small to permit the use of conventional loading methods; accordingly, the bend-type specimen shown in Fig. 13 was evolved. Initial experiments aimed at duplicating the flat-break bulge data using conventional testing machine loading were unsuccess-

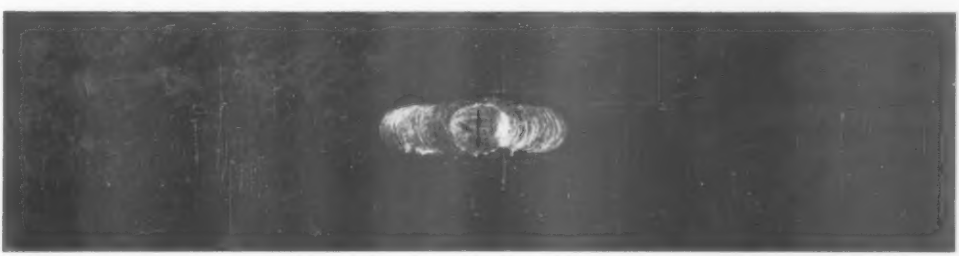

Fig. 13 Drop weight test specimen with crack-starter weld

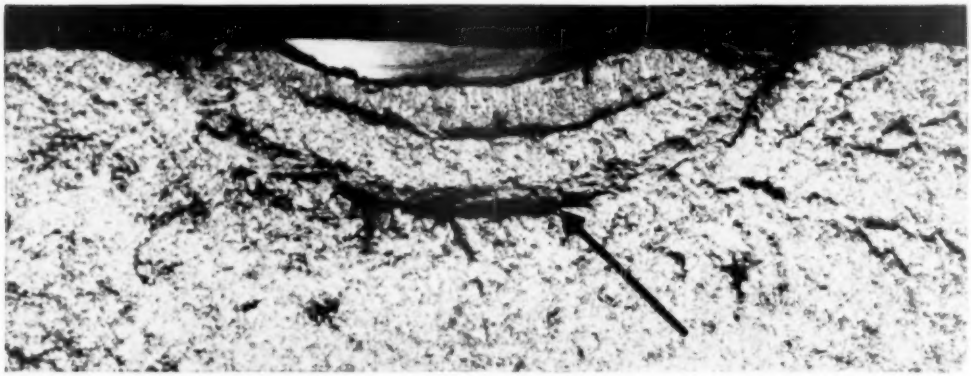

Fig. 14 Example of thin "finger nail" of shear resulting from the momentary unloading following the development of the initial crack in conventional testing machine bend test of "drop weight" test specimens

ful. The momentary unloading resulting from the development of the cleavage crack in the weld and its extension a short distance into the base metal resulted in a thin "finger nail" of shear, Fig. 14. Deformation was then required to resharpen the crack front. Impact loading by means of a heavy hand sledge was successful in eliminating the momentary unloading and indicated the suitability of a simple drop-weight system such as was finally evolved, Fig. 15.

The drop-weight test entails the delivery of an energy blow which develops a very small amount of deformation. The weight-drop conditions are adjusted so that approximately 1 to 2% lateral contraction is developed in the test specimen if a break does not occur. Tests are conducted over a series of temperatures with results indicated in Fig. 16. It was observed that all specimens which fractured developed approximately $^1/_8$% lateral contraction, which represents the amount of deformation required to fracture the weld; specimens which did not fracture developed the limiting amount of deformation determined by the energy input. Figure 17 illustrates typical "break" and "no break" specimens. The ductility transition is thus clearly defined by a simple and practical test procedure.

Table 3 summarizes the drop-weight, ductility transition temperatures determined for the various types of steels and the values of Charpy V energy obtained at the same temperature. It is evident that, irrespective of the orientation of the specimen, the Charpy V range of 5 to 10 ft-lb defines the temperature at which rimmed and semikilled steels are no longer capable of developing deformation in the presence of a cleavage crack. The

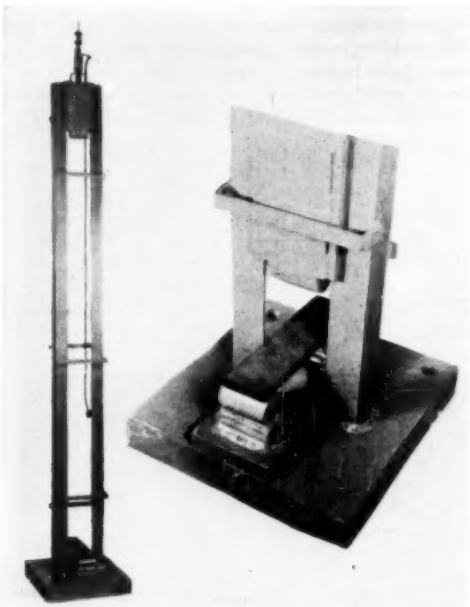

Fig. 15 Drop weight test equipment showing specimen in position on anvils

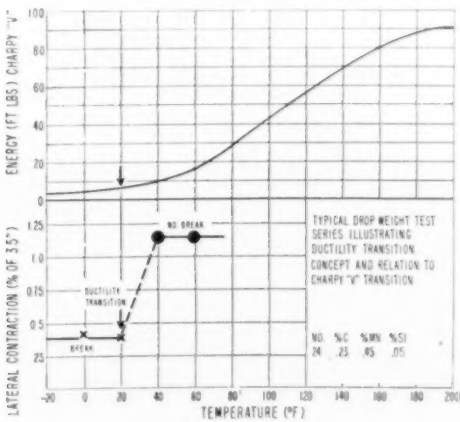
ENERGY (FT LBS) CHARPY "V"
LATERAL CONTRACTION (% OF 35")
TEMPERATURE (°F)
NO BREAK
DUCTILITY TRANSITION
BREAK
TYPICAL DROP WEIGHT TEST SERIES ILLUSTRATING DUCTILITY TRANSITION CONCEPT AND RELATION TO CHARPY "V" TRANSITION
NO. %C %MN %SI
24 .25 .65 .05

Figure 16

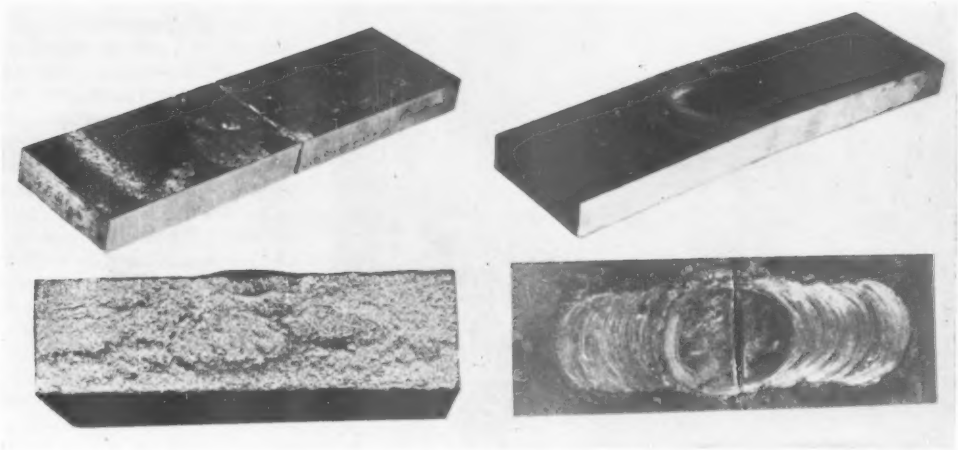

Fig. 17 Typical "break" and "no break" drop weight test specimens. Note weld crack which was not effective in developing brittle fracture in the contiguous base metal

critical range for the fully killed steels is indicated to be 8 to 15 ft-lb. This behavior is similar to that shown by the fracture transitions, i.e., the transitions occur at higher Charpy V energy values for fully killed steels.

Table 3—Charpy V and Keyhole Ft-Lb Energy Values at Ductility Transition Temperature of Drop Weight Test

Steel type	*Code*	*Ductility transition temperature, ° F* — *L*[a]	*T*[a]	*Charpy V, ft-lb*	*Charpy Keyhole, ft-lb*
Vanity HTS	1	0	0	9	
	2	+20		10½	40
	3	−20		13	38
	4	−20		6	20
	4N	−60		10½	
	5	−20	−20	7	
	6	−20	−20	7	
	7	−20	−20	6	
Fully killed	9	0		5	18
	9N	−40		9½	40
	9A	−20		9	5
	10	−20	−20	14	
	12	0	0	7	
	14	0		9	
	15	0	0	5	
	16	0		7	
	17	+40		6	
Semikilled	18	−20	−20	7	
	19	−20	−20	6	
	20	+20	+20	8	27
	21	0		5	30
	21A	+20		5	5
	22	+20		7	28
	22A	+40		4	8
	23	+20		7	10
	24	+20		6	20
	25	+20		5	15
	26	+20		5	15
	28	+20		8	10
Rimmed	29	0		4	
	30	0		4	
	31	+20		6	25
	33	+20		5	
	34	+20	+20	4	
	35	+20	+20	3	3

[a] *L* and *T* denote direction of crack propagation.

Figure 18 shows drop-weight fractures at 20° F (Charpy V, 5 ft-lb) for a semikilled ship steel of average quality, resulting from the presence of an arc-strike and of the crack starter. The equivalence of this common defect to the cleavage crack of the brittle weld is self-evident. Such equivalence is obtained only if the arc-strike is of a "clean" type, i.e., does not contain weld metal. Tests entailing arc-strikes which contained weld metal did not fracture at 20° F, indicating a protective effect due either to the presence of notch-tough weld metal or to the elimination of fine fissures in the crater area. The usual type of accidental arc-strikes which do not contain weld metal accordingly should be considered highly dangerous. Figure 19 provides an example of a ship failure originating from an arc-strike. It is observed that at the temperature of this failure the starting plate developed but 6 ft-lb Charpy V.

CORRELATION WITH SHIP FRACTURE DATA

The National Bureau of Standards Charpy V data of fractured ship plates[1] provide a means of evaluating the significance of the crack-starter tests. For this evaluation it is necessary to consider the steels which either originated from the same production groups used in Liberty Ship construction (rimmed and semikilled

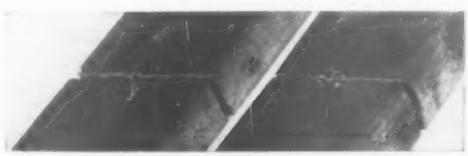

Fig. 18 Example of equivalence of arc strike to crack-starter weld at ductility transition (Steel No. 24, + 20° F)

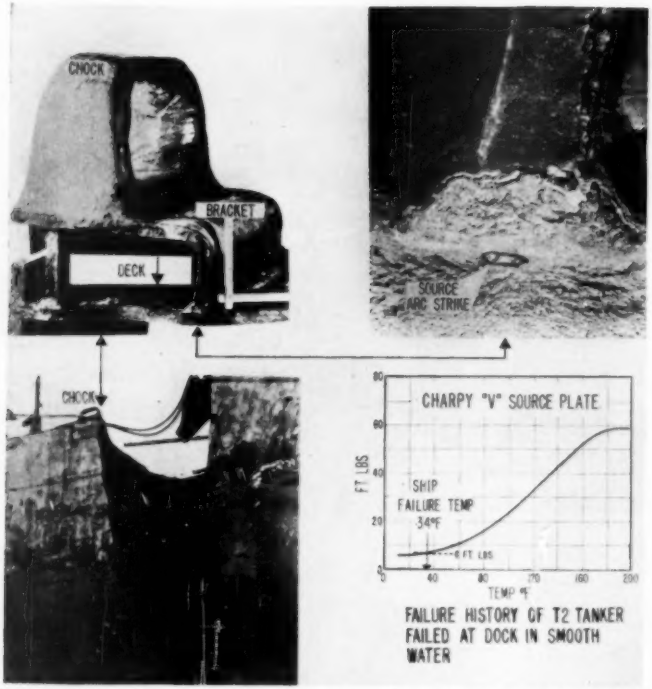

Fig. 19 Ship failure initiated by arc strike. Note that plate which initiated failure developed only 6 ft-lb Charpy V (ductility transition range of drop weight test) at temperature of failure

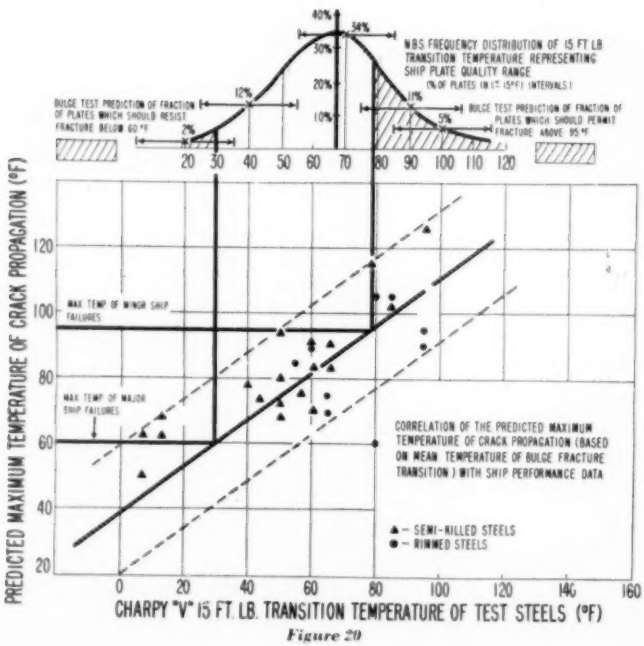
NBS FREQUENCY DISTRIBUTION OF 15 FT LB
TRANSITION TEMPERATURE REPRESENTING
SHIP PLATE QUALITY RANGE
(% OF PLATES IN 5°F INTERVALS)
BULGE TEST PREDICTION OF FRACTION
OF PLATES WHICH SHOULD RESIST
FRACTURE BELOW 60°F
BULGE TEST PREDICTION OF FRACTION OF
PLATES WHICH SHOULD PERMIT
FRACTURE ABOVE 95°F
MAX TEMP OF MINOR SHIP
FAILURES
MAX TEMP OF MAJOR
SHIP FAILURES
CORRELATION OF THE PREDICTED MAXIMUM
TEMPERATURE OF CRACK PROPAGATION (BASED
ON MEAN TEMPERATURE OF BULGE FRACTURE
TRANSITION) WITH SHIP PERFORMANCE DATA
▲ – SEMI-KILLED STEELS
● – RIMMED STEELS
PREDICTED MAXIMUM TEMPERATURE OF CRACK PROPAGATION (°F)
CHARPY "V" 15 FT. LB. TRANSITION TEMPERATURE OF TEST STEELS (°F)

Figure 20

steels) or are more recent steels of the same general type and analysis. The evaluation is based on the mutual relation of ship fracture data and the crack-starter tests fracture data and the Charpy V properties of the steels in question.

The NBS investigations separated the ship fracture plates into three groups—fracture "source," "through" and "end" plates. Frequency distribution plots of the 15 ft-lb Charpy V transition temperatures of these three groups indicated that the "source" plates represented the inferior portion of the over-all grouping, the "end" plates represented the best portion and the "through" plates were of intermediate quality. It was thus concluded that in ship failures the poorest plates initiated the failure, the average quality plates continued the failure and the best plates terminated it. The considerable overlap of the individual frequency distributions of the three types of plates indicated that depending on the temperature of service a portion of the plates which fell at the center of the over-all population could behave as "source," "through" or "end" plates. Such plates could be expected to serve as "source" plates at low temperatures, "through" plates at intermediate temperatures and as "end" plates at high temperatures. The probability of plates which fell at the extreme ends of the over-all population to behave as both "source" or "end" types appeared to be small.

Ship fracture history has indicated that major failures entailing the fracture of a great number of plates in direct line, did not occur above 60° F, and that partial failures entailing a small number of plates occurred in the range of 60 to 95° F. In the Board Investigation[4] it was established that the frequency of ship fractures increased sharply with decreased temperature below the range of 60 to 70° F.

The above items of information collectively indicate that at temperatures below 60° F the majority of the steels used in ships are capable of crack propagation ("through" types). In the narrow range of 60 to 70° F a sufficient number of steels develop resistance to crack pro-

pagation (become "end" types) to prevent the development of major failures. In other words the probability of a continuous line-up of "through" plates in the path of fracture becomes very low. This is particularly significant in that this temperature range is near the center of the range of ship operating frequency. The continuance of minor failures to 90–95° F indicates that a fair probability of having "source" and "through" plates exists to the maximum operating temperature range of ships.

The NBS investigation of the Charpy V properties of the three classes of plates *at the temperature* of failure disclosed the following:

1. "Source" plates developed an average of 5 to 8 ft-lb and a maximum of 11.4 ft-lb.

2. "Through" plates developed an average of 8 to 12 ft-lb and a maximum of 13 ft-lb (32% of the plates absorbed more than 10 ft-lb).

3. "End" plates developed an average of approximately 20 ft-lb and a maximum of 52 ft-lb. In some cases these plates permitted the propagation of brittle fracture to half-distance prior to stoppage (the 52 ft-lb plate, for example, which developed such a half-way crack at 55° F).

These data require allowance for possible inaccuracies in the reported temperature of service and variations in the properties of the steels at various positions in the plates. It is indicated, nevertheless, that the conditions of crack initiation differ considerably from the conditions of crack propagation and crack stoppage. A notch toughness level defined by the Charpy V specimen as greater than 11 ft-lb is sufficient to prevent the initiation of failure. However, the stoppage of brittle cracks once in motion requires a notch toughness which may be as high as 52 ft-lb, depending on the steel and probably on the amount of elastic strain energy available at the particular location of the ship.

The predictions of the crack starter tests for the rimmed and semikilled steels appear to be in general conformance with the ship fracture data as indicated by the summary comparison presented in Table 4. The ductility transition values determined by the drop-weight tests are compared to "source" plate data since both represent crack starting in the absence of appreciable deformation. The bulge fracture transitions based on the mid-point of the 0.010 to 0.020-in. surface shear lip range are considered to represent the average limiting condition of crack propagation; accordingly, these data are related to "end" plate data.

Table 4—Correlation with Charpy V Energy Values, Ft-Lb

	Crack-starter tests		*NBS ship plate fractures*	
	Avg	*Max*	*Avg*	*Max*
Crack initiation in the absence of appreciable deformation	4-6	8	5-8	11.4
Crack propagation prevented	15-30	70	12-20	52

The prediction of the crack-starter bulge tests relative to the fraction of the over-all population which should be *operative* as "through" plates at various service temperatures is indicated in Fig. 20. This plot relates the 15 ft-lb transition temperature of the rimmed and semikilled test steels to the temperature which represents the mid-point of the 0.010 to 0.020-in. shear lip range. The NBS frequency distribution of all plates based on 15 ft-lb transition is also shown. It is deduced from the relationships presented in Fig. 20 that at temperatures below 60° F (maximum temp. for major ship failures) only a small fraction (possibly 2%) of the total population may be effective as "end" plates thus providing a high probability of in-line arrangement of "through" plates required to develop complete fractures. In the range of 60 to 70° F it is predicted that an appreciable portion (possibly 20%) of the population becomes effective as "end" plates, thus providing the condition required for the prevention of major failures. It is also predicted that above 95° F (limiting temperature for minor failures) an appreciable (10 to 15%) part of the total population is still capable of serving as "through" plates. The failure of this portion of the population to provide fractures above 95° F in ship service may be explained by the infrequency of operation at such temperatures and by the small number of plates which are available to serve as "source" types.

SIGNIFICANCE OF KEYHOLE DATA

The widespread use of the Keyhole specimen for control and specification purposes has resulted in the accumulation of a large body of information relative to the quality ranges of various steels. However, it is difficult to use this information inasmuch as the only available correlation with ship performance is based on the Charpy V specimen. The dual use of the V and Keyhole specimens in this investigation and the demonstrated relation of the crack-starter tests to ship performance provide an opportunity to explore the significance of Keyhole data.

The general relationships of the bulge fracture transitions to the Charpy Keyhole energy and fracture transition curves are illustrated in Figs. 21–24. It is noted that the bulge fracture transitions occur at temperatures related to the upper "shelf" region of the Keyhole energy transition curves, lower portion of figures. It is difficult to correlate the transition features of the two tests inasmuch as the "shelf" region does not provide a suitable criterion of reference. The Keyhole fracture transition curves, upper portion of figures, have been separated by plotting each curve at a different level on the chart; each transition is plotted from 0 to 100% shear fracture and the amount of shear developed in the temperature range of the bulge fracture transition is denoted by the two percentage figures listed on the curve. It is observed that all bulge fracture transitions

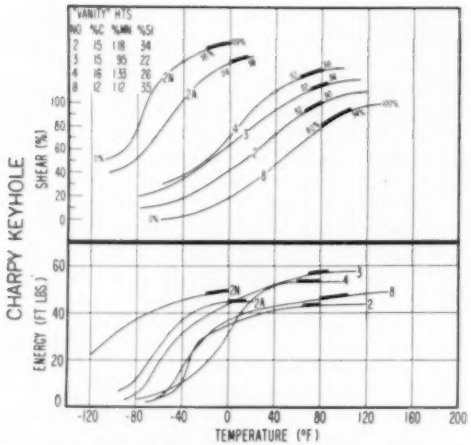

Fig. 21 Temperature range of bulge fracture transitions related to Keyhole transition curves

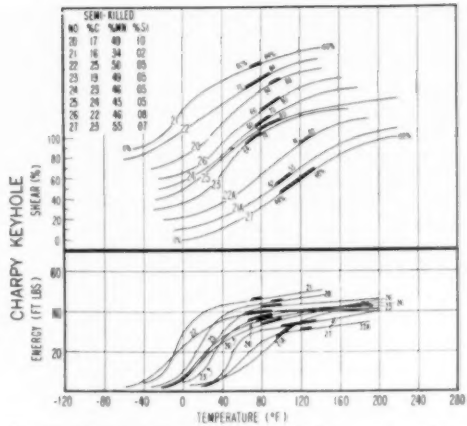

Fig. 23 Temperature range of bulge fracture transitions related to Keyhole transition curves

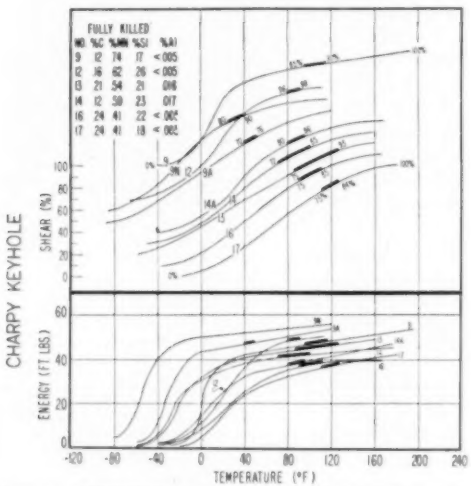

Fig. 22 Temperature range of bulge fracture transitions related to Keyhole transition curves

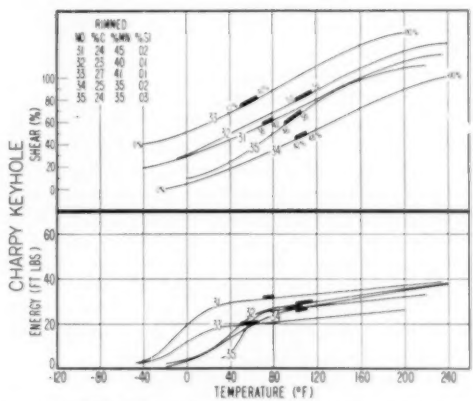

Fig. 24 Temperature range of bulge fracture transitions related to Keyhole transition curves

are developed in the temperature range of the Keyhole fracture transitions. As in the case of the Charpy V specimen energy transitions, the fully killed steels develop bulge fracture transitions at higher positions of the Keyhole fracture transition curve than the semi-killed and rimmed steels.

The relationship of the drop-weight test ductility transition temperature to the Charpy Keyhole energy values developed at the same temperature is summarized in Table 3. It is observed that a sharp definition to a limited range of energy values noted for the Charpy V specimen is not developed; very low and very high values are represented. This is explained by consideration of the relative characteristics of the Charpy V and Keyhole transition curves, Fig. 25. While the Charpy V energy curve falls gradually over the entire transition range the Keyhole energy curve shows a region (in the temperature range of 5 to 10 ft-lb Charpy V) which is characterized by the development of either high or low values. The high values represent approximately 10 to 15% shear in the fracture while the low values represent essentially complete cleavage. Vanderbeck and Gensamer [5,6] have shown that for structural steels most of the high values fall above 20 ft-lb, while most of the low values fall below 10 ft-lb. In view of this behavior it is customary to draw a mean curve through the center of the scatter band and define the Keyhole energy transition temperatures in terms of this curve. If this is done, Keyhole transition temperatures based on 5, 10, 15 or 20 ft-lb energy levels then represent relationships to the mean curve rather than temperatures which *exclusively* develop such energy values. Accordingly, the physical significance of temperature transitions based on the subject energy levels is not the same

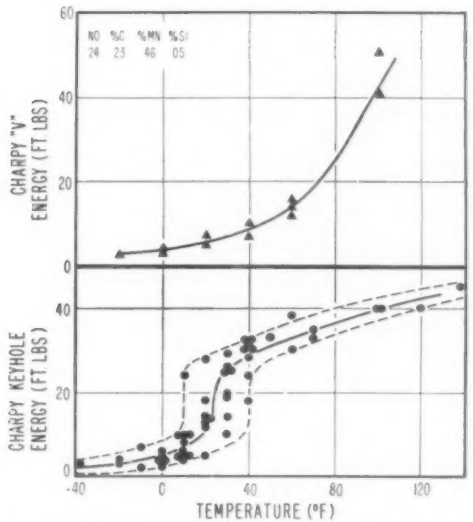

Fig. 25 Relative characteristics of Charpy V and Charpy Keyhole transition curves

for Charpy V and Keyhole specimens. This does not mean that the Keyhole transition is not satisfactory for notch toughness evaluations. Its relative value in this respect depends on the practicality of establishing the mean curve with the same reproducibility as is obtained with the Charpy V energy curve and the determination of the relationship of the curve to service failures such as has been determined for the Charpy V specimen curve. This question could be resolved by determining if a useful correlation exists between the Charpy V notch 10 ft-lb transition which has been demonstrated to correlate with ship failures and a particular Keyhole transition. The correlation of the drop-weight ductility transition with Charpy V 5 to 10 ft-lb values for semi-killed and rimmed steels and the lack of correlation with a similar narrow range of Keyhole energy values indicates on first consideration that the Charpy V and Keyhole specimens do not correlate. It should be observed, however, that the temperature range defined by Charpy V curves between the limits of 5 to 10 ft-lb is not significantly greater than the temperature range which spans the sharply falling Keyhole *average curve* drawn through the center of the scatter band. This fact suggests a correlation based on the mid-span of the scatter range of the Keyhole curve (same as inflection point of average curve) with the temperature of Charpy V 5 and 10 ft-lb criteria. The mid-span criterion for Charpy Keyhole has been used by Vanderbeck and Gensamer in recognition of the difficulty of determining the upper or lower limits of the Keyhole average curve. It should be noted that for ship plate steels the upper limit occurs at approximately 20 to 30 ft-lb, the lower limit at approximately 5 to 10 ft-lb and the inflection point passes sharply through 15 ft-lb. Figure 26 presents plots correlating the Charpy V 5 and 10 ft-lb transition temperatures with the Keyhole 15 ft-lb temperatures (equivalent to the mid-span position) for the steels used in this investigation and also Vanderbeck and Gensamer's correlation[5] of the Charpy V 10 ft-lb temperature with the mid-span position of the Keyhole curve. It is observed that the 15 ft-lb, or mid-span transition, of the Keyhole specimen occurs at a temperature which is approximately 20° F lower than the Charpy V 10 ft-lb transition and essentially equal to the temperature of the Charpy V 5 ft-lb transition.

These relationships indicate that the temperature range defined by the higher temperature half of the scatter band (20 to 40° F range in Fig. 25) developed by the Keyhole specimen correlates with the temperature range of 5 to 10 ft-lb Charpy V and accordingly is significant to service failures. It should be observed that the Charpy V 10 ft-lb criterion has the advantage of defining a *boundary* temperature above which crack starting does not occur in ships. The criterion of mid-span position of the Keyhole transition curve as suggested by Vanderbeck and Gensamer[5] defines a temperature

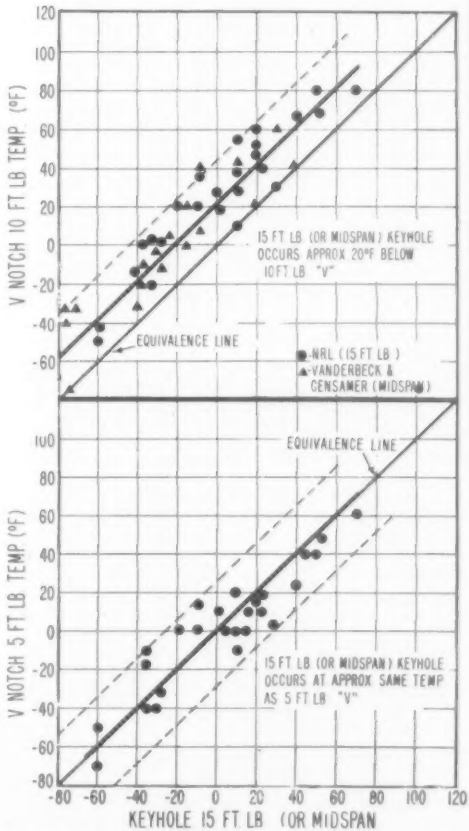
V NOTCH 10 FT LB TEMP (°F)
15 FT LB (OR MIDSPAN) KEYHOLE OCCURS APPROX 20°F BELOW 10 FT LB "V"
EQUIVALENCE LINE
NRL (15 FT LB)
VANDERBECK & GENSAMER (MIDSPAN)
V NOTCH 5 FT LB TEMP (°F)
EQUIVALENCE LINE
15 FT LB (OR MIDSPAN) KEYHOLE OCCURS AT APPROX SAME TEMP AS 5 FT LB "V"
KEYHOLE 15 FT LB (OR MIDSPAN

Fig. 26 Relationships of Charpy V 5 and 10 ft-lb transitions to Keyhole 15 ft-lb transition

which is approximately 20° F below the boundary temperature and accordingly provides no assurance of safety. In using the Keyhole mid-span or 15 ft-lb transition for design purposes it is thus necessary to introduce a temperature correction of at least +20° F.

In retrospect it appears fortunate that the NBS investigation of ship fracture plates was based on the use of the Charpy V specimen. The distinct differentiation between "source," "through" and "end" plates, for example, would not have been evident from Keyhole energy values.

CORRELATION WITH SCHNADT'S CONCEPTS

According to Schnadt's concepts[7] the critical conditions of service which a steel must withstand may be defined in terms of the average triaxiality which is to be expected in the particular service. The most severe conditions are represented by near-balanced triaxial stress systems of the type which are developed at the root of sharp cracklike defects; such stress conditions Schnadt terms "coheracic." The mildest conditions are presented by nontriaxial stress systems of the type which are developed on smoothly contoured surfaces; such stress conditions are termed as "dinacic." Intermediate stress systems of increasing average triaxiality are also defined by similar terms. Schnadt has developed a series of Charpy-like specimens which are deemed to develop these various stress systems. Thus, an unnotched specimen is considered to develop "dinacic" stresses while the specimen shown in Fig. 27 which features an extremely sharp pressed notch is considered to develop "coheracic" stresses. Specimens of intermediate notch acuities are used to develop intermediate stress systems. Another feature of these specimens is the use of a hard pin to replace the compression zone of the notch-bend test pieces.

The general theory of the Schnadt approach is based on the premise that various types of structures and service inherently develop a limiting stress condition which may be defined by one of the particular specimens. In view of the probable presence of sharp, cracklike defects in welded structures it is considered that the steel must be capable of resisting "coheracic" stresses to the lowest temperature of service. For a riveted structure the probability is for the worst defect to be of less severe nature; hence, the steel is required only to resist a stress system of lower average triaxiality. In general, these views are not novel, Schnadt has merely defined a variety of stress conditions on a basis of the demands these various stress systems are expected to make on the inherent resistance to fracture of the steel. Stress systems of a critical nature are deemed to have "low plastifying power," i.e., to promote brittle fracture. Noncritical stress systems are deemed to have "high plastifying power," i.e., to promote ductile behavior. Thus, the inherent "plastifying" tendencies of the stress system are matched against the inherent plasticity of the metal and the quality of the metal is judged accordingly.

To use Schnadt's terminology, this investigation is concerned with evaluating the inherent plasticity or resistance to brittle fracture of ship steels for service conditions entailing stresses of lowest possible "plastifying power," i.e., "coheracic" stresses developed at the root of the sharp cleavage cracks. Schnadt's procedure for the evaluation of the temperature below which the steel is unable to resist brittle fracture in the presence of cleavage cracks is based on a critical energy value on the "coheracic" specimen—temperature transition curve. Schnadt claims, based on extensive engineering experience, that the steel is "coheracic" (not subject to brittle fracture) only at temperatures for which the energy absorption is greater than 2 Kg-M/cm², this value corresponds roughly to 4 ft-lb. Figure 28 presents a

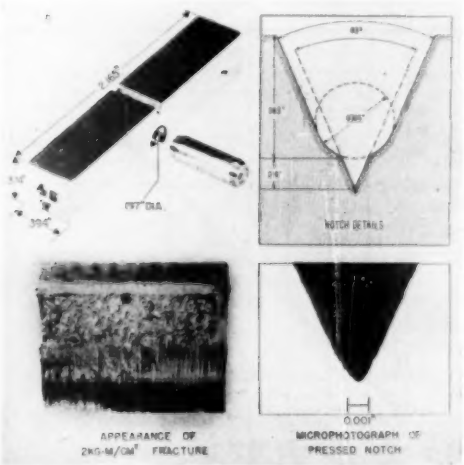

Fig. 27 Details of Schnadt "coheracic" test specimen

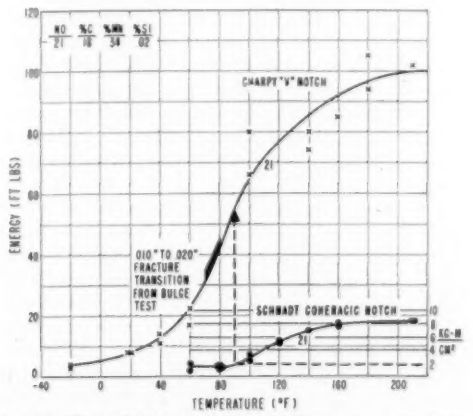

Fig. 28 Typical Schnadt "coheracic" specimen transition curve illustrating method of relating lowest coheracic temperature (2 Kg-M/cm²) to bulge fracture transition and Charpy V transition curve

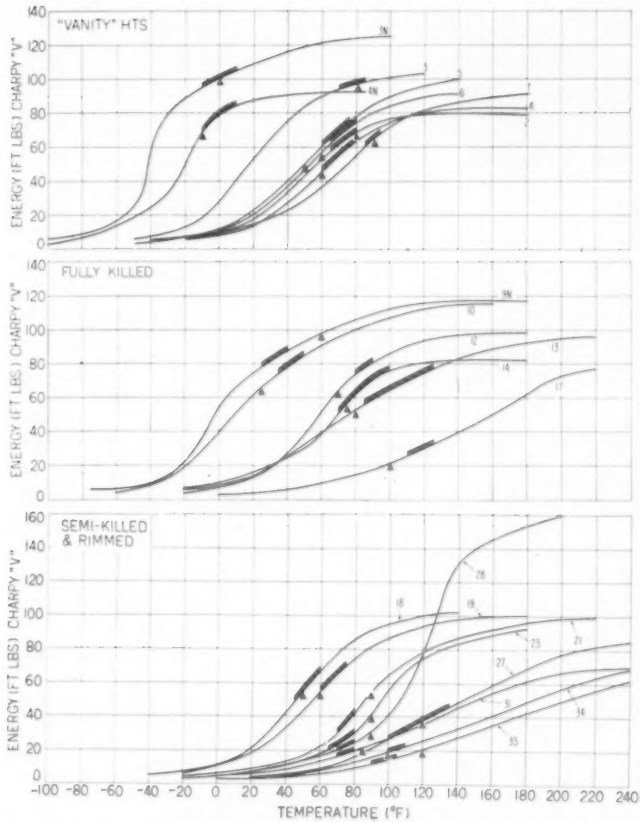
"VANITY" HTS
FULLY KILLED
SEMI-KILLED & RIMMED
ENERGY (FT LBS) CHARPY "V"
TEMPERATURE (°F)
-100 -80 -60 -40 -20 0 20 40 60 80 100 120 140 160 180 200 220 240

Fig. 29 Summary of Schnadt "coheracic" temperature-bulge fracture transition relationships

typical Schnadt "coheracic" specimen transition curve (details provided in Appendix B) in comparison to a Charpy V transition curve (both specimens LH type). The relation of the lowest "coheracic" temperature to the 0.010-to 0.020-in. shear lip temperature range of the crack-starter bulge test is also indicated; Fig. 29 presents a summary of all comparison tests which were made. It is apparent that the Schnadt predictions of the upper temperature of possible catastrophic failure are in general agreement with the bulge test predictions. The two ultra-sharp-notch specimens are in mutual agreement in indicating that the use of an invariant Charpy V energy level for the evaluation of the brittle fracture characteristics of steels of different deoxidation practice is not possible. Apparently, the improvement obtained by the use of fully killed steels as compared to rimmed and semikilled steels is not as great as predicted by the shift in the Charpy V transition curves; the same comments apply to Keyhole transition curves.

Figure 30 presents an example of the application of the Schnadt and crack-starter concepts to the performance of a large-scale test structure. The composite illustration serves to demonstrate that the mutual predictions of the crack-starter and Schnadt tests for the brittle failure of Box Girder No. 1 at 80° F (WRC Investigation [8,9]) were fulfilled.

PERFORMANCE OF IMPROVED MATERIALS

If the correlations between the performance of the rimmed and semikilled steels in the crack-starter tests and the performance of these steels in ship fractures are considered valid, predictions relative to the performance of improved ship steels are of considerable interest. In order to generalize the predictions of the crack-starter tests it is necessary to consider the Charpy V* notch toughness quality of the improved steels in relation to the NBS data[1] for the ship fracture steels. Furthermore, it is essential to differentiate between the crack initiation and crack propagation aspects of the brittle fracture problem.

If one considers that crack initiation is the controlling aspect ("cracks which do not start cannot run" theory) it is feasible to consider that improvement of the present class of rimmed and semikilled steels to the extent of eliminating the "start" portion of the population would suffice. This would mean eliminating all steels which develop less than 10 ft-lb V at 0° F, which may be taken as the lowest temperature of service. Analysis of the NBS data indicates this means the elimination of possibly 95% of the group, which does not appear to be a practical procedure. Metallurgical improvement of the class as a whole by shifting the average quality (10 ft-lb V distribution) of the group to 30–40° F lower temperature appears to be a more feasible approach. From another viewpoint a selection procedure could be established by means of Charpy V or drop-weight test specifications assuring that suitable steels are used at critical positions in the ship. Inasmuch as a great preponderance of failures initiated at specific critical positions on the ships this process would serve to protect these positions. The feasibility of such a procedure naturally depends on the nature of shipyard operations. Such a procedure would modify the need

* *Note:* The discussion is restricted to Charpy V criteria because of the limitations of the available data for Keyhole specimens.

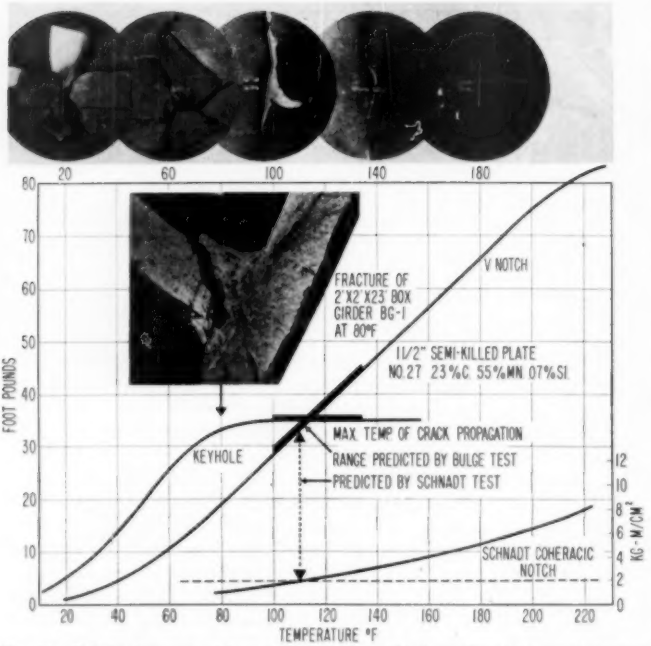

Fig. 30 Brittle fracture of box girder No. 1 at 80° F predicted by crack-starter and Schnadt tests

for a full 30–40° F shift in the average quality level of the present steels.

In general the drop-weight tests indicate that a shift in the 10 ft-lb V temperature develops an essentially equivalent shift in the temperature critical to crack starting. The shift is somewhat less if a fully killed steel is compared to a semikilled or rimmed type, but the difference in the relative shifts is not great. Thus, in so far as crack starting is concerned, the Charpy V 10 ft-lb temperature may be taken as a practical criterion irrespective of steel type.

The limited number of steels of various improved types available for this investigation does not permit broad generalizations as to the shift in the Charpy V 10 ft-lb transition which may be achieved in comparison to the wartime ship steels. The normalized, "Vanity" HTS steels appear to be the only type which develop sufficiently low 10 ft-lb transition temperatures to indicate that the over-all population of the group may be expected to resist fracture initiation at subzero temperatures; the fully killed (Si and Si-Al), low-carbon, high-manganese types show promise when in the normalized condition.

Vanderbeck[6] has presented Charpy Keyhole data for a large number of as-rolled plates representing production heats covering a broad range of thicknesses, analyses and deoxidation practices. The average Keyhole midspan transition temperature for the various types is summarized in the form of the upper graph of Fig. 31. These data were adjusted by a + 20° F shift to represent the Charpy V 10 ft-lb transition on a basis of the correlation which has been described, and replotted on the lower graph. If the various assumptions and correlations involved are valid, certain interpretations may be made as to the temperatures below which these various steels should be expected to behave as "source" types, i.e., permit crack initiation under elastic loading conditions:

1. Steels of *average quality* classed as Liberty ship type should be capable of crack initiation over a wide range of temperatures ranging from 0 to 80° F, which agrees with service experience. The drop-weight ductility transitions for steels of this type (3/4-in. to 1-in. thickness only) ranged from 0 to 40° F.

2. The ABS-B grade steels of *average quality* should not permit crack initiation above 0° F. Two 3/4-in. steels of this type which were tested showed drop-weight ductility transitions of −20° F.

3. The ABS-C grade steels of *average quality* should

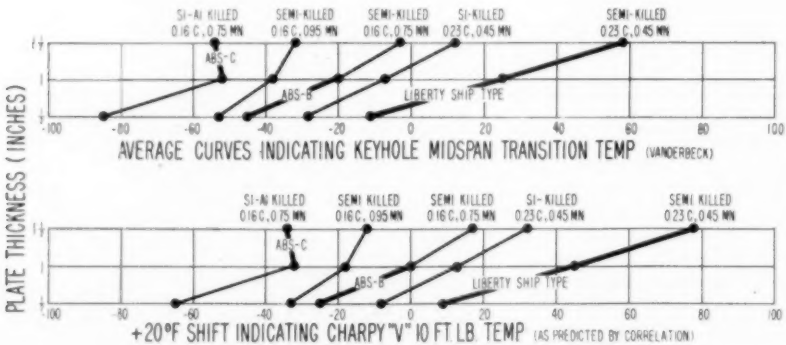
SI-AL KILLED 0.16 C, 0.75 MN
SEMI-KILLED 0.16 C, 0.95 MN
SEMI-KILLED 0.16 C, 0.75 MN
SI-KILLED 0.23 C, 0.45 MN
SEMI-KILLED 0.23 C, 0.45 MN
ABS-C
ABS-B
LIBERTY SHIP TYPE
AVERAGE CURVES INDICATING KEYHOLE MIDSPAN TRANSITION TEMP (VANDERBECK)
PLATE THICKNESS (INCHES)
+20°F SHIFT INDICATING CHARPY "V" 10 FT. LB. TEMP (AS PREDICTED BY CORRELATION)

Fig. 31 Application of Keyhole transition data for the evaluation of of the crack initiation characteristics of various steels

not permit crack initiation above −30° F. No steels of this type were available for test.

If one considers that *crack propagation* is the controlling aspect of brittle fracture, the Charpy specimen becomes a less reliable tool for assessment of the relative fracture characteristics of different types of steels. As discussed previously a given temperature shift in the lower portion of the Charpy V curve (5, 10 or 15 ft-lb criteria) does not result in equivalent shift in the maximum temperature of brittle fracture. This behavior is related to the general tendency of rimmed and semikilled steels to limit crack running to temperatures encompassing the lower half of the Charpy V transition range while fully killed steels in general permit crack running to temperatures in the upper half of the transition range. In this respect the decrease in the highest temperature of brittle fracture obtained by the use of the fully killed steels investigated, as compared to semikilled and rimmed steels is disappointing. Only the normalized HTS "Vanity" type steels indicated shifts in the maximum temperature of crack running to near-zero temperatures.

RELATION OF MATERIAL AND STRUCTURAL FACTORS

It is recognized that any generalization of the over-all problem of catastrophic failure of large welded structures requires consideration of the factors of design, fabrication and materials. It is possible, but not necessarily feasible, to achieve a solution to this problem by eliminating deficiencies associated with only one of these three factors. The material problem, for example, is inherent to the unfortunate sensitivity of our common structural steels to a change in temperature of only 20 to 40° F which, by perversity of chance, falls in the center range of atmospheric temperatures. If the transition from residual ductility sufficient to prevent the propagation of fractures to near-zero ductility which permits brittle fracture had occurred at the lower range of normal atmospheric temperatures (−20 to 0° F), the problem would not exist. This suggests that the improvement required in our common structural steels is not very great. It does not necessarily imply, however, that this may be achieved with ease, inasmuch as economic and production factors must be considered for large tonnage materials. In this respect the significance of small specimen transitions is a vital issue, for the extent of improvement which may be obtained by feasible heat treatments, modifications of mill practice, analysis adjustments, etc., can be prejudged by practical methods only by the use of such specimens. Unless the significance of small specimen test data is understood, it is not possible to arrive at sound decisions as to the most feasible and most economical methods of improving our present materials. It is in this respect that the major efforts of this investigation have been directed.

The effectiveness of improvements in design and fabrication adopted during the last years of wartime ship construction has been demonstrated by the significant reduction in the number of failures for this group of vessels. In general these improvements relate to minimizing the demands which are made on the notch ductility of the metal. To use Schnadt's convenient terminology, this means "plastifying" the design, i.e., eliminating stress features which reduce the inherent ability of the metal to resist brittle fracture. Poor design such as the sharp corners of the original Liberty ship hatch design places inordinate demands on the weldability and inherent notch toughness of the steel. As the result of poor design, plastic deformation may develop at constraint regions which operate under triaxial stress conditions. If the total deformation developed exceeds the ductility of the weld or the HAZ (weldability problem) a crack is developed. At this point only the inherent notch toughness of the ship plates in the presence of the defect serves to prevent total failure. The presence of fabrication defects within the weld or the HAZ may be expected to intensify deficiencies of design and weldability.

Obviously, there are practical limits to improvements related to design, weldability and fabrication. Inasmuch as design must be functional, it is not possible to eliminate openings, corners and other structural discontinuities. The problem of effecting a weld without developing a damaged HAZ is far from complete solution. If one considers the great variety of conditions under which welds are actually made and the limited knowledge we presently have of weldability of different steels as indicated by even a simple bead-on-plate weld, it must be concluded that the actual quality of practical HAZ is difficult to predict.

The practical limitations which are inherent to any possible single method of obtaining a solution to this problem indicated the need for a combined approach based on obtaining feasible improvements in design, fabrication and materials. It is particularly desired to record this opinion in view of the limitations of the scope of this investigation to the material aspects of the over-all problem.

SUMMARY

1. A technique has been developed to investigate the inherent tendencies of steels for the initiation and propagation of brittle fracture in the presence of an initial cleavage crack. The technique is based on the development of the cleavage crack in a bead-on-plate of brittle hard-surfacing weld metal during the loading of the test plate. The development of the initial cleavage crack is independent of the properties of the test material. Loading of the test plates is accomplished by the use of explosives for studies concerned with crack propagation. Drop-weight impact loading is used for studies concerned with crack initiation in the absence of appreciable deformation.

2. The specific resistance to crack initiation of the steel under test is indicated by its relative ability to

develop deformation in the presence of the initial cleavage crack in the brittle weld. At a temperature defined as the *ductility transition* the fractured test plate shows no deformation, indicating the immediate development of a brittle failure concomittantly with the development of the weld crack. Above this temperature brittle fracture occurs only if continued deformation is developed in the test plate following the formation of the cleavage crack in the weld. This behavior is considered significant to the problem of crack initiation in welded structures which contain fabrication defects of the nature of cleavage cracks and which are designed such as not to develop appreciable deformation. Accordingly, the steels used in such structures are deemed to resist the initiation of brittle failure until the temperature falls to the critical ductility transition level.

3. It is demonstrated that rimmed and semikilled ship plate steels of the type used in Liberty ships develop ductility transitions at temperatures for which the Charpy V energy falls below 10 ft-lb (range 3 to 8 ft-lb) which is in agreement with the "source" plate data of fractured ships. Fully killed steels develop ductility transitions at temperatures for which the Charpy V energy may be as high as 15 ft-lb (range 5 to 14 ft-lb). Thus, the improvement in the resistance to the initiation of brittle fracture obtained by the use of fully killed steels is slightly less than indicated by the Charpy V transition criterion of 10 ft-lb.

4. The specific resistance to the propagation of brittle fracture is indicated by the development of shear lips on the fracture surfaces. At the ductility transition temperature the amount of shear lip is essentially nil; however, with increasing temperature the shear lips become distinctly visible reaching a thickness of 0.050 to 0.080 in., above which brittle fracture is no longer possible even with the high amount of elastic energy available with the use of explosion loading. The level of 0.010 to 0.020 in. was taken as the *fracture transition* in keeping with the observation that this thickness range is the maximum developed in ship fracture plates. The fracture transition temperature range is deemed to represent the temperature range above which the propagation of brittle fractures in conventionally loaded large welded structures is no longer possible.

5. Correlation with the temperature range of major and minor failures of ship fractures and with "end" plate data of fractured ships indicates good correspondence with the fracture transitions of rimmed and semikilled steels of the type used in ship construction.

6. Rimmed and semikilled steels develop fracture transitions at temperatures which generally fall in the range of 25 to 50% energy of the Charpy V transition curve. Fully killed steels generally develop fracture transitions in the range of 50 to 75% energy. Thus, the increased resistance to the propagation of brittle fracture obtained by the use of fully killed steels is considerably less than indicated by the Charpy V transition curves.

7. The ductility transition is not affected by orientation of the direction of fracture with respect to the rolling direction. The fracture transition occurs generally at 10 to 15° F higher temperature for fracture propagation in the direction of rolling as compared to fracture propagation transverse to the direction of rolling.

8. The Charpy Keyhole specimen also shows significant correlation with the ductility and fracture transitions. The energy transition curve of the Keyhole specimen is not suitable for correlation with the fracture transition, since this occurs at temperatures which correspond to the essentially flat upper shelf of the Keyhole curve. The fracture appearance transition of the Keyhole specimen correlates with the crack propagation test fracture transition; rimmed and semikilled steels correlate generally with 40 to 80% shear while fully killed steels correlate with 70 to 100% shear. The ductility transition occurs generally at temperatures 20° F above the 15 ft-lb (or mid-span) Keyhole energy transition.

9. The Schnadt "coheracic" specimen featuring an ultra sharp pressed knife edge notch develops 2 KgM/cm² energy (specified by Schnadt as the transition criterion indicating the critical temperature below which brittle fracture is possible in welded structures) at temperatures which are generally in close agreement with the fracture transitions. This correspondence holds for all types of steels investigated.

ACKNOWLEDGMENTS

The technical advice and guidance during the course of the investigation and in the preparation of the report provided by Dr. O. T. Marzke, Superintendent of the Metallurgy Division, is gratefully acknowledged. The assistance in conducting various experiments provided by C. G. Ackerlind and J. Davenport is acknowledged with appreciation.

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Appendix A

DETAILS OF CRACK-STARTER TESTS

The crack-starter test plate shown in Fig. 1 is pre-

pared for welding by milling a groove $^1/_2$-in. wide, $^5/_{32}$-in. deep and $2^1/_2$-in. long. The groove is arc welded by means of two short beads started at each end of the groove and completed at the center; crater cracking is minimized by this technique. A thin ($^1/_{16}$-in.) rotating abrasive disk is used to cut a notch in the weld metal. The notch is cut to half depth of the weld and to within $^1/_8$-in. of the weld edges. Hard surfacing electrodes of the low-alloy, high-carbon type are best suited for crack starting. A proper electrode of this type develops a crack only at the notch position. Excessive brittleness to the extent that secondary cracks are developed at other positions in the weld is undesirable. For this reason alloy white-iron, hard-surfacing electrodes are considered unsatisfactory. The weld is considered fully effective if it develops a cleavage crack when the base metal reaches its yield point.

The test plates are heated or refrigerated to the desired test temperature, placed over a circular die, Fig. 2, and loaded by the gas pressure developed by an explosive charge detonated in air above the plate. The explosive charge (4-lb Pentolite wafer) is positioned 24 in. above the plate by means of an expendible cardboard box. The force of the gas pressure serves to develop a small bulge on the unsupported area of the plate. The bulge depth resulting from this combination of explosive weight and distance is approximately $^3/_4$ to 1 in. depending on the thickness of plate and the tensile strength of the steel. The use of greater loads serves no useful purpose inasmuch as the excess energy results only in driving the fragments against the base plate if brittle failure follows or in developing shear tears if brittle failure is not possible.

The shear lips developed on the fracture surfaces were first measured by means of a graduated stage microscope. The measuring process was later expedited by means of a technique based on comparing the width of the lip to the thickness of feeler gages; all measurements were made by a single individual. The technique provided reliable measurements for lips greater than 0.005 in.—lips of less than this amount were not measured. The rapid increase in the thickness of shear lips in the 20° F steps used in the investigation precluded the possibility of significant measurement errors. The various branches of the fracture developed shear lips of uniform thickness.

The drop-weight specimen was prepared in exactly the same fashion as described above. Surface grinding of the sides was required for lateral reduction measurements; these measurements were performed using micrometer calipers. The conditions of the sides are not critical, saw or flame-cut edges may be used for routine testing.

The drop-weight equipment was fabricated with angle iron sections for the guides and a heavy plate for a base. The drop weight was constructed from a steel block; a hardened steel round welded to the block served as the striking tup; small bars welded to the sides of the block served to provide easy fit alignment with the channel sections during the drop. The anvil section was constructed from a steel plate with hardened steel rounds for the specimen supports; these were spaced 12-in. between centers. The dimensions of the equipment or of the specimen are not critical. The drop weight used weighed 60 lb and was released from a height of approximately 6 ft. Because of the nature of the test, the energy release does not need to be exactly reproducible. In fact, wide differences in the height of the drop were found not to affect the test results as long as the drop was sufficient to develop *at least* the $^1/_8$% lateral contraction needed to produce the weld crack.

It should be recorded that in nearly 1000 tests entailing bulge and drop-weight specimens, made in this and other investigations under way, no case of failure of the weld to develop a brittle fracture has been observed. The brittle weld has been shown to be fully effective to temperatures at least as high as 200° F (hig'h-carbon steels) for both the bulge test and the drop-weight test. It should also be noted that in nearly 75 bulge transition series (75 × 6-8 specimens or approximately 600 tests) including alloy and cast steels tested to date, no reversals have been observed in the continuous smooth change of the fracture pattern and appearance with temperature.

Appendix B

DETAILS OF SCHNADT SPECIMEN TESTS

The Schnadt specimens were prepared in conformance to the requirements outlined in Reference 7. The V notch was first machined to 0.063 in. in conventional fashion; the knife-edge notch was added by pressing a tungsten carbide tool into the base of the V notch. This operation was performed by means of an accurate alignment jig using tensile testing machine loading at controlled rate (0.01 ipm). The depth of the complete notch was held to close tolerance (0.079 ± 0.002 in.) by means of a dial gage; microscope check readings were made for approximately 50% of the specimens. The compression pinhole was made by a reaming operation with tolerances of 0.197 + 0.0005 − 0.0000-in. The hardened steel compression pins were ground to close tolerances of 0.1965 + 0.0003 −0.0000 in. A standard Charpy machine was used for testing; the ft-lb values obtained were plotted directly and a conversion scale was used to denote the KgM/cm^2 values required for the evaluation of the test results.

Inert-Gas Shielding of the Metallic Arc

Burn-off rates and spatter losses in inert-gas-shielded metal-arc welding with carbon steel electrodes

by William L. Green and Robert J. Krieger

Summary

An investigation was conducted to study burn-off rates and spatter losses accompanying the shielding of the arc of $^{5}/_{32}$- x 14-in. carbon steel electrode (polished core wire) with inert gases. Burn-off rates and spatter losses of bare polished core wire used without shielding are included for the sake of comparison. In addition to using different gas shielding media several sets of data were taken for varying current and voltage ranges on both straight and reverse polarity direct current.

The higher currents which can be used with inert-gas shielding produced much greater burn-off rates than lower currents usable with no shielding. Also, much lower spatter losses were noted for the shielded arcs. In addition, it was found that straight polarity currents produced higher burn-off and lower spatter loss than reverse polarity, all other conditions remaining the same.

Two types of metal transfer in the inert-gas-shielded consumable-electrode arc were observed on high-speed motion pictures. The actions of the arc can also be observed quite well on these pictures. The high-speed picture also serves as a means of comparing or noting the differences in the arc effected by the different shielding gases. The sources of spatter could also be fairly well determined from the films.

INTRODUCTION

DURING recent years much work has been done in developing the newest welding process utilizing an inert-gas shield with consumable electrode. This process was initially developed to weld aluminum and magnesium. Most recently attempts have been made to extend the development of this process to weld stainless and mild steels.

As far as the operation itself is concerned, it is known that steel can be successfully welded with this process. However, there are certain effects of this process which do not allow the resulting weld to meet the metallurgical and physical standards demanded of a weld, and so easily obtainable with other widely used processes. The most undesirable effects of the process are porosity and low ductility.

This investigation involves the use of fairly large diameter core wire of the type now used for commercially coated electrodes of the E-60 series, where as the majority of work done to date utilized small diameter core wire.

The main objective of this initial phase of the investigation was to determine the operating characteristics such as burn-off rate and spatter loss. No attempt has yet been made to study weld quality or a means of improving ductility or decreasing porosity. This is to be handled in a later phase.

EQUIPMENT

All welding was done with an electronically controlled welding head. The apparatus is shown in the photograph of Fig. 1. This system controlled about a preset arc voltage with an average error of approximately ± 0.5 v for the shielded arcs and approximately ± 1.0 v for the unshielded arc. The term "unshielded" is used to identify the arc when no shielding gases other than the atmosphere are used. The term "atmospheric arc" may also be used to identify the same condition.

The power supply, a 300-amp d c rectifier type, was selected because its static and dynamic characteristics were quite identical as compared to any motor-generator set where the characteristics may differ quite widely. A static load, a resistor bank of nichrome heating elements, was used to set the machine at a given operating point, i.e., 24 v and 150 amp.

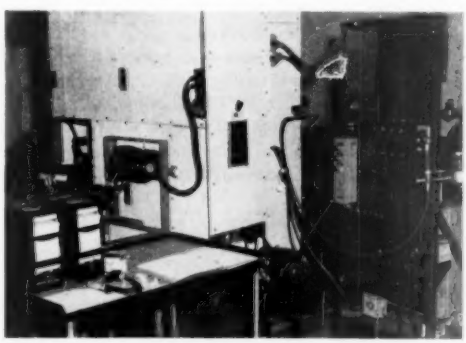

Fig. 1 Control panel with welding head shield

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Presented at the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Feb. 15, 1953.

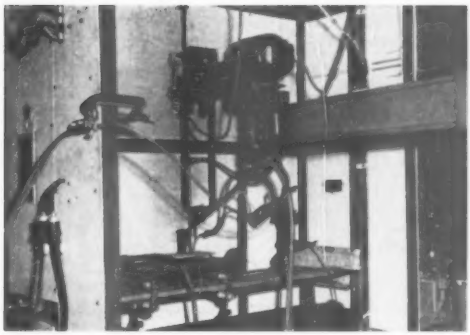


Fig. 2 Welding head and gas nozzle assembly

The shielding gases were introduced around the arc through a modified Heliarc HW-4 torch assembly as shown in the photograph of Fig. 2. Gas flow was controlled by standard flowmeters. In tests involving a mixture of argon and helium, the mixing chamber of a small oxy-acetylene torch was utilized as a method of mixing the gases ahead of the nozzle.

Current and voltage values were taken from recording meters which also were used in conjunction with the static load to obtain any desired operating condition.

Metal transfer, spatter and arc action were recorded on film with a high-speed camera at an exposure rate of approximately 3500 frames per second. Simultaneous oscillograms of current and voltage were taken during several of the operations.

MATERIAL ANALYSIS

The analysis of the electrode material and the plate material is as follows:

	Electrode	*Plate*
Carbon, %	0.11	0.23
Manganese, %	0.41	0.48
Silicon, %	0.02	0.03
Phosphorus, %	0.01	0.012
Sulfur, %	0.024	0.044

Test plates were sheared from 1-x $^3/_8$-in. bar stock.

Although the above analyses have little to do with the results of the investigation as reported in this article, it was deemed wise to include such analyses in the event this investigation be carried further to determine the effect on weld quality.

SHIELDING GASES

The following gases or mixtures of gases were used as shielding media: Argon, standard purity; helium, standard purity; argon-helium, 50-50 mixture; sigma-argon (approximately 5% oxygen). The rate of gas flow was set at 40 to 50 cfh for all tests. Although it would undoubtedly be desirable to investigate several other ratios of the mixtures of argon-helium and argon-oxygen, the time limit imposed on this phase of the investigation made it impossible to do so.

TEST PROCEDURE

Since it was desirable to keep plate preparation at a minimum a bead on bar test was used with all welding being done in the flat position with the electrode held vertical.

Electrodes were weighed in lots of 25 each and the average weight taken as the weight of each electrode. All plates were weighed and stamped prior to welding. About 8 to 10 in. of a 14-in. rod were burned off during each test. The welding time was determined by means of a manually operated stop watch.

After removing all spatter, each weld piece was carefully weighed as was the electrode stub. All weights were recorded to the nearest tenth of a gram. Hence, the data now recorded were used to calculate burn-off, deposition, and spatter loss.

It was decided to take data over ranges of both current and voltage on both straight and reverse polarity d-c operation with each shielding medium used. The current was held constant while the voltage was varied by a few volts for each test in a series of tests. In contrast the voltage was held constant while the current was varied for each test in the series. This resulted in quite a range of operating conditions from which to gather data.

TEST RESULTS

All data have been graphed using straight-line plots, as calculated by the least squares method, to provide a clearer presentation.

Burn-off Data

The general effect of shielding is to increase the burn-off rate considerably. However, the actual increase is due to the higher currents that can be used with a shielded arc. The effect of the shielding gases relative to the polarity of the current is of particular interest. Reverse polarity produces a higher rate of burn-off than straight polarity with the unshielded arc for any given set of current and voltage conditions. However, for each of the shielding gases used, the opposite is quite true. Straight polarity produced the higher burn-off rate in each case. In some cases an increase of 50% can be noted in the plots of Fig. 3.

It is also noticeable that the burn-off rate increased quite sharply as the current was increased in each case. Another interesting observation is that the range of operation, i.e., current variation, is much greater in most cases where the arc is shielded. The exceptions to this are the helium, straight polarity and argon-helium, straight polarity arcs.

Figure 3 also shows that, regardless of polarity, the

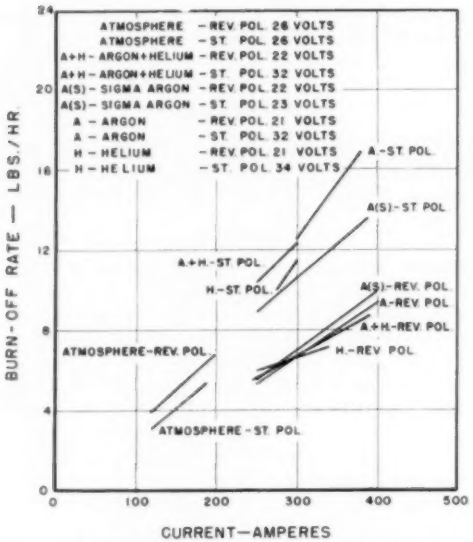

ATMOSPHERE – REV. POL. 26 VOLTS
ATMOSPHERE – ST. POL. 26 VOLTS
A+H – ARGON+HELIUM – REV. POL. 22 VOLTS
A+H – ARGON+HELIUM – ST. POL. 32 VOLTS
A(S) – SIGMA ARGON – REV. POL. 22 VOLTS
A(S) – SIGMA ARGON – ST. POL. 23 VOLTS
A – ARGON – REV. POL. 21 VOLTS
A – ARGON – ST. POL. 32 VOLTS
H – HELIUM – REV. POL. 21 VOLTS
H – HELIUM – ST. POL. 34 VOLTS
A-ST. POL.
A(S)-ST. POL.
A+H.-ST. POL.
H.-ST. POL.
A(S)-REV. POL.
A-REV. POL.
A+H.-REV. POL.
H.-REV. POL.
ATMOSPHERE-REV. POL.
ATMOSPHERE-ST. POL.
BURN-OFF RATE — LBS./HR
CURRENT—AMPERES

Fig. 3 Burn-off rates vs. arc current with voltage held constant

burn-off rate obtained with a mixture of argon-helium is much greater than that obtained with helium, but less than that obtained with argon. This seems to indicate that the argon is predominant, which is in accord with the findings of Prof. T. B. Jones in his paper, "Characteristics of the Tungsten Arc in Argon-Helium," AIEE Electric Welding Conference, April 18, 1952.

Figure 4 is a series of plots of burn-off rate vs. arc voltage; the tests where the current was held constant and the voltage varied. Operating at a higher arc voltage, i.e., longer arc, decreased the burn-off rate very slightly with the exception of sigma-argon where the increase is considerably sharper.

Spatter Data

Figure 5 is a series of plots showing spatter loss as percent of burn-off vs. current. It can readily be seen that the greatest spatter loss occurred with the unshielded arc. The spatter loss is seen to increase quite sharply with current, especially with the unshielded arc. There are two cases where the spatter shows a tendency to decrease with increase in current. The amount of data obtained in these two cases—argon-helium and sigma-argon straight polarity—may not be sufficient to present a true picture, however.

Plots of spatter loss vs. arc voltage are shown in Fig. 6. When the current was held constant and the voltage increased, the spatter loss increased sharply with the exception of sigma-argon straight polarity. It is also interesting to note that reverse polarity produced higher spatter losses than straight polarity.

Three types of spatter loss are recorded on the high-speed motion pictures of the various arcs. Spatter is noted to originate from the puddle in the form of fine

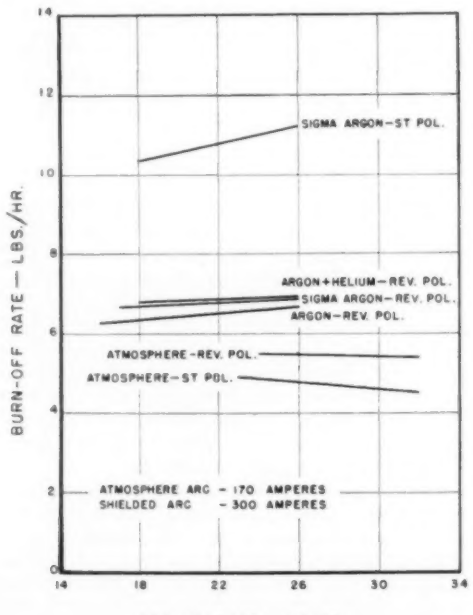
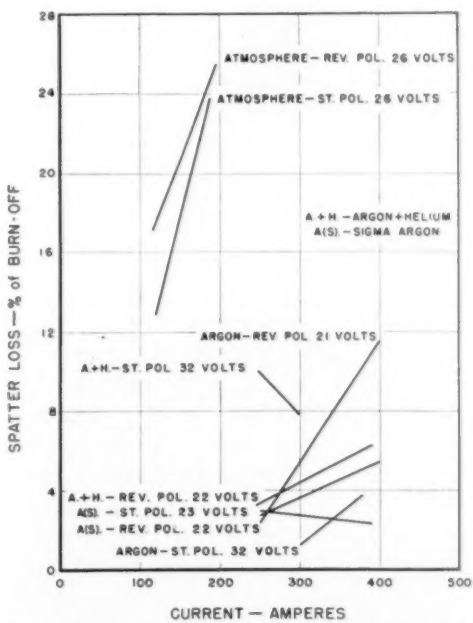

SIGMA ARGON – ST. POL.
ARGON+HELIUM – REV. POL.
SIGMA ARGON – REV. POL.
ARGON – REV. POL.
ATMOSPHERE – REV. POL.
ATMOSPHERE – ST. POL.
ATMOSPHERE ARC – 170 AMPERES
SHIELDED ARC – 300 AMPERES
BURN-OFF RATE — LBS./HR.
ARC VOLTAGE — VOLTS

Fig. 4 Burn-off rates vs. arc voltage with current held constant


ATMOSPHERE – REV. POL. 26 VOLTS
ATMOSPHERE – ST. POL. 26 VOLTS
A+H – ARGON+HELIUM
A(S) – SIGMA ARGON
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A+H.-REV. POL. 22 VOLTS
A(S) – ST. POL. 23 VOLTS
A(S) – REV. POL. 22 VOLTS
ARGON – ST. POL. 32 VOLTS
SPATTER LOSS — % of BURN-OFF
CURRENT – AMPERES

Fig. 5 Spatter loss vs. arc current with voltage held constant

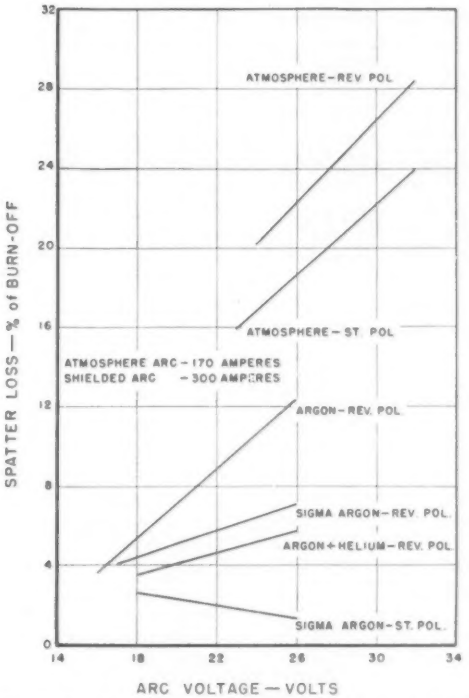

Fig 6 Spatter loss vs. arc voltage with current held constant

particles of metal traveling at extremely high velocities and for considerable distances. These particles were found to be very porous indicating that this may be one method by which gas escaped from the puddle. Fine spatter also emanated in much the same manner from the molten globule on the electrode.

A third source of spatter exists when metal transfer occurs through short-circuiting of the arc. The reignition of the arc is usually so violent that large particles of metal are hurled away from the puddle and the end of the electrode.

It would be difficult to determine which source produces the greatest amount of spatter. However, it is believed that the majority of it comes from the puddle.

METAL TRANSFER

Two distinct types of metal transfer were observed during this investigation. One type involves the build-up of a molten globule on the end of the electrode which increases in size until it becomes dislodged from the electrode and moves to the puddle as a free globule, i.e., not in contact with either the electrode or puddle. Voltage variations were noted on oscillograms indicating this transfer: however, the arc is maintained either around or through the globule.

The other type of transfer occurs when the molten globule build-up on the electrode contacts the puddle while still attached to the electrode. During the resulting short circuit of the arc the metal transfers across the molten column from the electrode to the work. As the transfer of the fused metal nears completion the molten column decreases in cross section until the current density reaches a value which breaks the column and the arc is re-established.

The type of transfer occurring seems to be a function of the arc length. Short-circuit transfer occurs with short arc lengths while free-globule transfer accompanies longer arc lengths. Between these two conditions exists a range of arc lengths yielding a combination of the two types. These transitional arc lengths and their corresponding voltages, producing free globule and short-circuit transfer, do not have the same values for different gases and it is indicated they are not constant for a given gas shield but vary with the current. Additional information will be required to establish the actual transitional arc voltages for the various gases at different currents. The preceding discussion is based on the limited number of observations which were possible in connection with this investigation.

CONCLUSION

It is apparent that the introduction of an inert gas as a shielding medium produces pronounced changes in the behavior of the metallic arc and that these changes are not due to merely excluding the atmosphere but involve the electrical and thermal characteristics of the gas.

Reverse polarity current produced a stable arc when it was shielded with all of the gases except helium. Helium shielding resulted in an unstable arc, making control within the desired limits extremely difficult. Straight polarity current resulted in an unstable arc requiring higher arc voltages to maintain suitable welding characteristics. The single exception to this straight polarity characteristic was sigma-grade argon. The addition of oxygen to argon stabilized the arc appreciably when using reverse polarity as well as straight polarity current.

Although the data are not conclusive, the free-globule type transfer exhibited better welding characteristics than did the short-circuit type. The globules transfer at extremely high velocities and, although all welding in this investigation was done in the flat position, it would seem that all position welding could be done satisfactorily.

The speeds of travel attainable with the shielded arc were considerably higher than those used with the atmosphere arc, but straight polarity yielded very light penetrations.

The reverse polarity shielded arc, allowing the use of high currents with the corresponding high rates of burn-off, low spatter loss, adequate penetration, and high speeds of travel, should find many practical applications in the welding industry. The straight polarity

arc exhibits higher rate of burn-off, lower spatter loss, less penetration and allows slightly greater speeds of travel. This is an excellent means of welding light gage sheet.

ACKNOWLEDGMENT

The authors wish to acknowledge the assistance of R. B. McCauley, Chief Chemist, Columbia Tool Steel Co., for his aid in providing the analyses of the materials. Special thanks are also accorded to R. B. McCauley, Jr., Instructor, and Prof. R. S. Green of the Department of Welding Engineering for their advice and encouragement.

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Porosity in Mild Steel Weld Metal

Discussion by D. L. Mathias

"POROSITY in Mild Steel Weld Metal" as reviewed by Messrs. Warren and Stout in the Welding Research Supplement of the August 1952 WELDING JOURNAL is a valuable contribution to our knowledge of welding. This review, coupled with others which have appeared in the JOURNAL, serves to provide a better understanding of the mechanics of welding that can only reflect in ultimate advancement of the science. Obviously, the completeness and accuracy of such reviews are limited to the extent of the references available and to interpretations of the results of the various investigators quoted.

Some portions of the material presented in the subject review are apt to create false impressions in the absence of factual data to the contrary while other portions of the review cover material which are of a controversial nature. The following comments, based on metallic arc welding with covered electrodes, are offered for the purpose of supplementing the review and also as discussion of some of the statements lest they work to the detriment of a specific product, process or procedure.

ROLE OF DEOXIDIZERS

Deoxidation as it affects the quality of arc-deposited welds has been recognized and practiced in this country since the introduction of covered electrodes which was long before Blomberg. Not only has deoxidation been accomplished singly by the use of Mn and Si but multiple deoxidation employing Ti, Zr and other elements is old in the field of electrode design.

D. L. Mathias is connected with the Arcrods Corp.

Paper by Donald Warren and R. D. Stout was published in THE WELDING JOURNAL, **31** (8), Research Suppl. 381-s to 386-s (1952).

While deoxidizers are decidedly beneficial in promoting weld soundness, they must be used judiciously with due regard to their influence upon the mechanical properties as well as the elimination of porosity. Overdeoxidation may be more detrimental to weld quality than the presence of a slight amount of porosity. Quite complex deoxidation systems are employed with many electrodes to arrive at a balance of weld soundness and mechanical properties required to meet rigid specification requirements.

MULTIPASS WELDS

"Hot" electrodes (EXX20) have been used successfully for over 20 years to deposit multipass welds in heavy plate to the highest radiographic standards. This electrode was of European origin and in its original form, considerable trouble was experienced with porosity. It was completely redesigned to meet American requirements and, in its present form, welds of acceptable soundness are obtained with little difficulty. The ease of obtaining sound welds with "hot rods" surpasses that of most types of electrodes and is equal to that of the low-hydrogen type.

Rutile-type electrodes are not noted for their ability to deposit radiographically sound welds, nor do existing specifications impose such a requirement. They are intended primarily for high-speed welding of light materials and for work with poor fit-up. For this class of work they perform admirably. Some European versions of the rutile rod deposit welds comparable in quality to American "hot-rods" but at a considerable sacrifice in operating characteristics according to our standards of evaluation. Sensitivity to arc length, a highly fluid slag which restricts their use and low deposition rate are among the limitations of this electrode when compared to "hot-rods" of the EXX20 class.

PREVENTION OF POROSITY

Avoiding porosity by "eliminating gas-producing impurities from the arc through the use of moisture free welding materials" touches upon a highly con-

(*Continued on page 595-s*)

Effect of Stresses on Strength of Circumferentially Welded Cylinders

Residual welding stresses in circumferential butt-welded pipe are inconsequential and can be relieved mechanically when loaded over as-welded yield strength

by L. J. Privoznik

Abstract

An investigation has been made to determine the effect of residual stresses and stress relief on the mechanical properties of mild steel cylinders containing circumferential butt welds.

It was found that under high internal pressures, in cylinders in which the parent metal was of equivalent strength to the weld metal, the circumferential band of weld metal in the as-welded cylinder resisted the expansion and increased the bursting strength of the cylinder to a greater degree than did the band of weld metal in the stress-relieved cylinder. In cylinders in which the parent metal was of greater strength than the weld metal the circumferential band of weld metal had little or no effect on the expansion and bursting strength of as-welded compared with stress-relieved cylinders.

Circumferentially butt-welded cylinders with high longitudinal restraint had considerably lower ductility and somewhat lower strength than did similar cylinders with normal restraint when tested at a temperature of −40° F.

The effect of residual stresses existing in the weld metal is believed to be inconsequential since the data indicate that the residual stresses can be relieved mechanically, during the first instance of loading over the as-welded yield strength, with no subsequent deterioration of mechanical properties.

INTRODUCTION

IT IS generally believed that residual stresses produced during the welding operation are detrimental and will result in a reduction in the strength and ductility of the weldment. Residual stresses in weldments are commonly reduced in magnitude by means of thermal stress-relieving treatments. However, the decrease in strength of the weldment due to the thermal treatment often is ignored.

Several investigations[1, 2] have been conducted to determine the magnitude and distribution of residual stresses in circumferential butt welds in pipe. The results of these investigations indicated that as-welded pipe had high-tensile longitudinal stresses (25,000 to 30,000 psi) at the inner surface of the weld and equally high compressive stresses at or near the outer surface of the weld. The circumferential stresses were found to be relatively low in magnitude (5000 to 15,000 psi); compressive at or near the inner surface and tensile at or near the outer surface. Stress relieving at 1200° F for 1 hr effectively reduced the magnitude of the longitudinal stresses but had only a small effect on the circumferential stresses.

The stresses and their distribution were assumed to have been due to bending moments produced by the shrinkage of the circumferential band of weld metal in the as-welded pipe.[2] These bending moments might be of a favorable nature should the pipe be subjected to high internal pressure, since the bending moments would tend to resist expansion forces due to high internal pressure; a stress-relieving heat treatment might nullify this condition. The present investigation has been carried out to determine the effects of residual stresses and of stress relief upon the mechanical properties of circumferentially butt-welded mild steel cylinders tested under high internal pressure at room temperature, 75° F, and at abnormally low temperature, −40° F.

EXPERIMENTAL PROCEDURE

Five mild steel cylinders, 6.60 in OD, 0.235 in. wall thickness and 24 in. over-all length, and four medium carbon steel cylinders 10.63 in. OD, 0.375 in. wall thickness and 33 in. over-all length, were prepared, each containing a circumferential butt weld, in the center of the cylinder, made with E-6011 electrodes in accordance with the conditions shown in Table 2. The chemical compositions and hardness of the pipe material and weld metal are shown in Table 1. All welds were single-V 60 deg included angle butt welds welded without backing straps.

SR-4 strain gages were cemented on the outer surface, in longitudinal and circumferential directions, at various locations along the weld of cylinders Nos. 1, 3, 6, 7, 8

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Table 1—Chemical Composition and Hardness of Parent Metal and Weld Metal

Material	Chemical composition, % C	Mn	P	S	Si	Hardness, BHN
6.60-in. OD pipe	0.15	0.44	0.025	0.02	0.12	105–110
10.63-in. OD pipe	0.35	0.88	...	...	0.27	160–172
E-6011 weld metal (as-welded)	0.12	0.40	0.011	0.027	0.13	120–130

and 9 and on the base metal $2^1/_2$ in. from the weld center line on cylinders Nos. 1 and 3. SR-4 Type A-7 strain gages were used on cylinders Nos. 1 and 3 and SR-4 Type Pa-3 (postyield) strain gages were used on the remaining cylinders.

All cylinders except cylinder No. 5 were hydrostatically loaded under two cycles of pressure. During the first pressure cycle loading was applied until a pressure corresponding to the yield strength of the material was reached; pressure was then released. During the second pressure cycle loading was applied until failure occurred. Cylinder No. 5 was loaded to failure under one cycle of pressure.

Due to the type of restraint on cylinders Nos. 6 and 7 (see Fig. 1b) the additional stresses produced by the internal pressure resulted in a circumferential to longitudinal stress ratio in the cylinder of 3:1. In the absence of restraint in the remaining seven cylinders (see Fig. 1a) the internal pressure produced a circumferential to longitudinal stress ratio of about 2:1. Cylinders Nos. 7, 8 and 9 were tested at a temperature of −40° F, and the other six cylinders were tested at a temperature of 75° F. A schematic diagram of the equipment used for testing cylinders under high pressure at room temperature is shown in Fig. 2 and for testing cylinders under high pressure and low temperature is shown in Fig. 3.

The strain gages, due to their high sensitivity, were able to indicate very slight yielding of a magnitude which was not revealed by the pressure gage.

To obtain a temperature of −40° F in the cylinder a solution of water and alcohol was circulated through the cylinder and through a heat exchanger connected in series with the cylinder. The heat exchanger consisted of a number of copper coils immersed in a mixture of dry ice and acetone. A layer of dry ice, packed in the insulated protective shield, aided in maintaining the low temperature during testing.

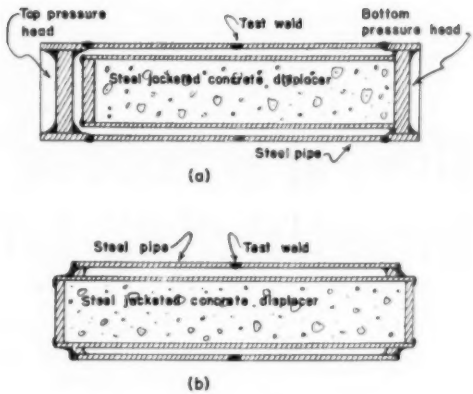

Fig. 1 Cross-sectional diagrams of cylinders having circumferential to longitudinal stress ratios of 2:1 (a), and 3:1 (b), during testing

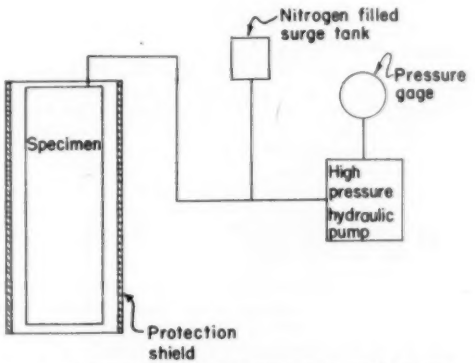

Fig. 2 Schematic diagram of equipment used for testing cylinders at high internal pressure and at room temperature

Table 2—Preparation of Cylinders

Spec. No.	Pipe OD, in.	Wall thickness, in.	Welding procedure	Remarks
1	6.60	0.235	400° F preheat, 1200° F stress relief for 2 hr.	Weld reinforcement removed
2	6.60	0.235	400° F preheat, 1200° F stress relief for 2 hr	Weld reinforcement retained
3	6.60	0.235	No preheat, no stress relief	Weld reinforcement removed
4	6.60	0.235	No preheat, no stress relief	Weld reinforcement retained
5	6.60	0.235	No preheat, no stress relief	Weld reinforcement removed
6	10.63	0.375	No preheat, no stress relief	Welded under high longitudinal restraint, reinforcement removed
7	10.63	0.375	No preheat, no stress relief	Welded under high longitudinal restraint, reinforcement removed
8	10.63	0.375	No preheat, no stress relief	Weld reinforcement removed
9	10.63	0.375	400° F preheat, 1200° F stress relief for 2 hr	Weld reinforcement removed

RESULTS

Strain data obtained during testing of the cylinders and plotted against pressure are presented in Figs. 4 through 9. The strain data are the actual strain readings. Principal stresses, producing the measured strains can be obtained by using the following relationships:

$$\epsilon_c = \frac{1}{E}(S_c - \mu S_l)$$

$$\epsilon_l = \frac{1}{E}(S_l - \mu S_c)$$

where ϵ_c and ϵ_l are the measured circumferential and longitudinal strains, S_c and S_l are the principal circumferential and longitudinal stresses, μ is Poisson's ratio and E is Young's modulus.

Determinations of the principal stresses (see sample

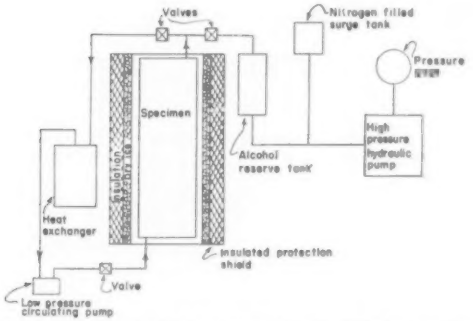

Fig. 3 Schematic diagram of equipment used for testing cylinders at high internal pressure and at low temperature

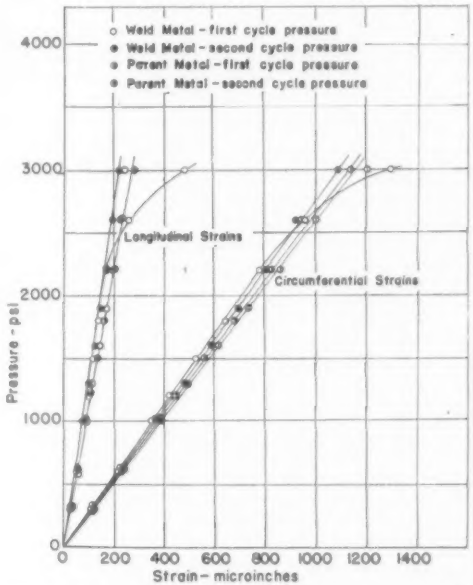

Fig. 5 Changes in strain with pressure for as-welded cylinder, cylinder No. 3

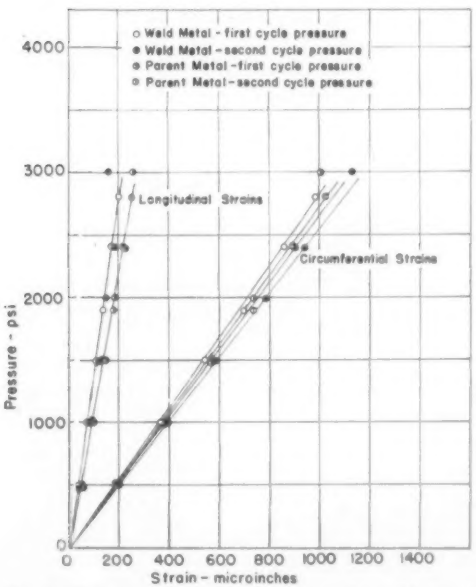

Fig. 4 Changes in strain with pressure for welded and stress-relieved cylinder, cylinder No. 1

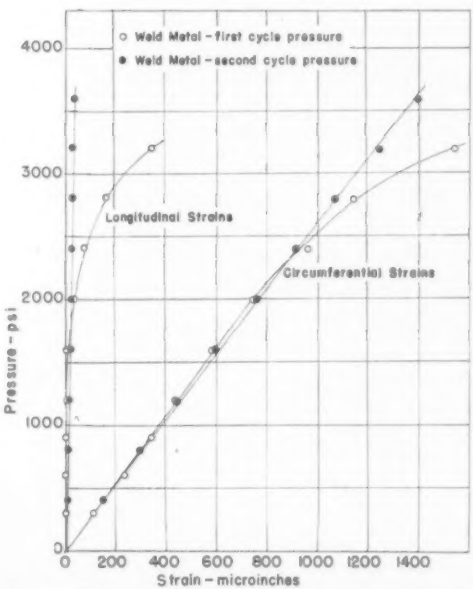

Fig. 6 Changes in strain with pressure for as-welded cylinder, cylinder No. 6, tested at 75° F under high longitudinal restraint

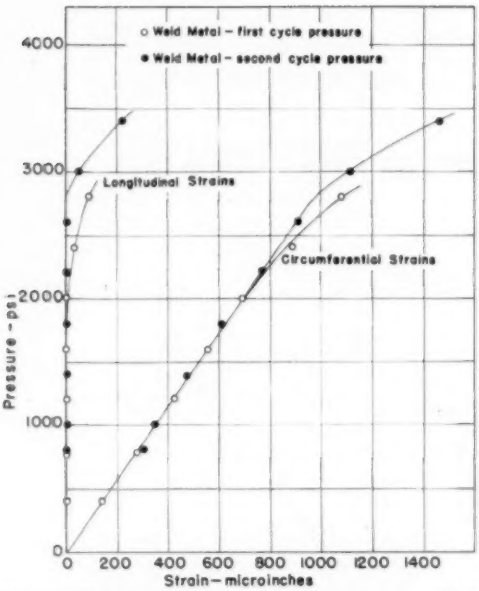

Fig. 7 Changes in strain with pressure for as-welded cylinder, cylinder No. 7, tested at −40° F under high longitudinal restraint

calculations) show that the circumferential to longitudinal stresses are in the ratio of 3:1 for cylinders Nos. 6 and 7 and about 2:1 in the remaining cylinders.

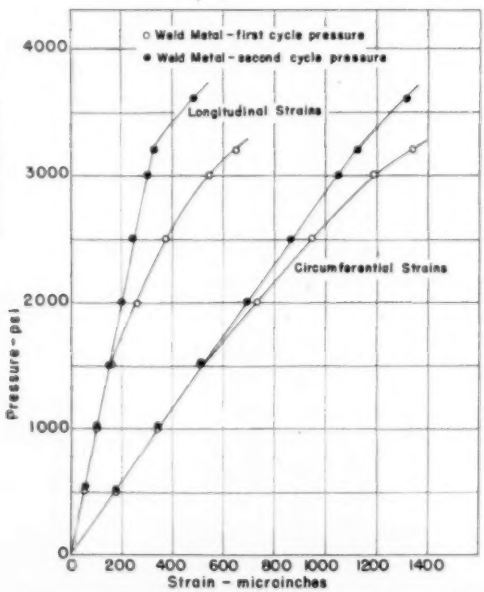

Fig. 8 Changes in strain with pressure for as-welded cylinder, cylinder No. 8, tested at −40° F

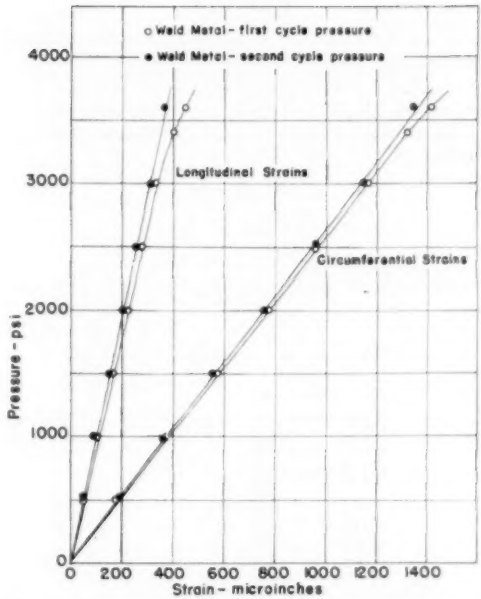

Fig. 9 Changes in strain with pressure for stress-relieved cylinder, cylinder No. 9, tested at −40° F

Sample Calculations

Determinations of the principal (circumferential and longitudinal) stresses from the measured strains taken from the pressure-strain curves for Cylinder No. 6 (Figs. 7) and Cylinder No. 1(Fig. 4) are shown below.

For Cylinder No. 6:

Circumferential strain, $\epsilon_c = 600$; longitudinal strain, $\epsilon_l = 25$.

$$\epsilon_c = 600 = \frac{1}{E}(S_c - \mu S_l) \qquad (1)$$

$$\epsilon_l = 25 = \frac{1}{E}(S_l - \mu S_c) \qquad (2)$$

Solving (1) for S_c in terms of S_l

$$S_c = 600E + \mu S_l \qquad (3)$$

Substituting (3) into (2) and solving for S_l

$$S_l = \frac{(25 + 600\,\mu)}{1 - \mu^2} E = 226E \qquad (4)$$

Substituting (4) into (3) and solving for S_c

$$S_c = 600E + \mu\, 226E = 678E \qquad (5)$$

The ratio of the principal stresses, therefore, is

$$\frac{S_c}{S_l} = \frac{678E}{226E} = 3.0 \qquad (6)$$

For Cylinder No. 1:

Circumferential strain, $\epsilon_c = 900$; longitudinal strain, $\epsilon_l = 220$.

$$\epsilon_c = 400 = \frac{1}{E}(S_c - \mu S_l) \qquad (7)$$

$$\epsilon_1 = 80 = \frac{1}{E}(S_1 - \mu S_c) \qquad (8)$$

Solving (7) for S_c in terms of S_1

$$S_c = 400E + \mu S_1 \qquad (9)$$

Substituting (9) into (8) and solving for S_1

$$S_1 = \frac{(80 + 400\ \mu)}{1 - \mu^2} E = 220E \qquad (10)$$

Substituting (10) into (9) and solving for S_c

$$S_c = 400E + \mu\ 220E = 466E \qquad (11)$$

The ratio of the principal stresses is

$$\frac{S_c}{S_1} = \frac{466E}{220E} = 2.1 \qquad (12)$$

A summary of the mechanical properties of the welded cylinders after testing is given in Table 3. Standard Charpy V-notch transition temperature data for the materials tested at low temperature are presented in Fig. 10 (weld metal and heat-affected zone included only in the as-welded condition).

DISCUSSION OF RESULTS

Interpretation of Strain Gage Data Obtained During Cyclic Loading

The pressure applied during the first pressure cycle for each cylinder was limited to a level corresponding approximately to the yield strength of the material, thus minimizing excessive cold working of the material.

The pressure-strain curves (Figs. 4 and 9), for the stress-relieved weld metal, and for the parent metal 2.5 in. from the weld in both the as-welded and stress-relieved cylinders (Figs. 4 and 5), are essentially straight and parallel up to a pressure corresponding to the yield strength of the material for the two pressure cycles.

The pressure-strain curves for the weld metal in the as-welded cylinders (Figs. 5 through 8) indicate that yielding occurred at lower pressures during the first pressure cycle than during the second pressure cycle and also at lower pressures than for either of the two

Table 3—Summary of Mechanical Properties of Pressure-Tested Welded Cylinders

Spec. No.	Remarks	Test temp., °F.	Stress ratio, C/L	Reduction in wall, thickness of weld, %	Circumferential elongation, Weld, %	Circumferential elongation, Base metal, %	Circumferential pressure, Yielding, psi	Circumferential pressure, Failure, psi	Unit[a] yield, stress, psi	Unit[a] rupture, stress, psi	Failure
1	Stress-relieved, no reinforcement. Two-pressure cycles	75	2:1	31	14.0	15.0	3200[b]	4500	45,000	63,000	Rupture in center of cylinder
2	Stress-relieved, reinforcement retained. One-pressure cycle	75	2:1	..	9.6	12.0	3500[b]	4950	49,000	69,500	Failure due to small tear in central weld
3	As-welded, no reinforcement. Two-pressure cycles	75	2:1	26	9.8	12.7	2200[c] 3400[b]	5000	30,900 47,700	70,000	Rupture in center of cylinder
4	As-welded, reinforcement retained. One-pressure cycle	75	2:1	..	4.5	6.5	3700[b]	5300[d]	52,000	74,000	Premature failure due to small tear in head weld
5	As-welded, no reinforcement. One-pressure cycle	75	2:1	..	5.6	7.1	3500[b]	5100	49,000	71,500	Failure due to small tear in central weld
6	As-welded. Longitudinal restraint. Two-pressure cycles	75	3:1	13.3	6.0	6.8	1800[c] 2800[b]	6550	25,600 39,800	92,800	Failure initiated in weld propagated across length of cylinder
7	As-welded. Longitudinal restraint. Two-pressure cycles	−40	3:1	1.6	3.7	3.7	2000[c] 2800[b]	6450	28,400 39,800	91,500	Failure initiated in weld propagated across length of cylinder
8	As-welded. Two-pressure cycles	−40	2:1	11.2	8.2	8.2	1600[c] 3200[b]	6950	22,700 45,500	98,500	Failure initiated in weld propogated across length of cylinder
9	400° F preheat, 1200° F stress relief. Two-pressure cycles.	−40	2:1	11.2	8.0	8.0	3200[b]	6900[e]	45,500	98,000	Failure did not occur

[a] Unit stresses (yield and rupture) calculated from hoop stress formulas which are based on elastic loading.
[b] Yielding as indicated by fluid pressure gage.
[c] Yielding as indicated by SR-4 electric resistance strain gages.
[d] Maximum pressure attained.
[e] Maximum pressure obtained but failure did not occur.

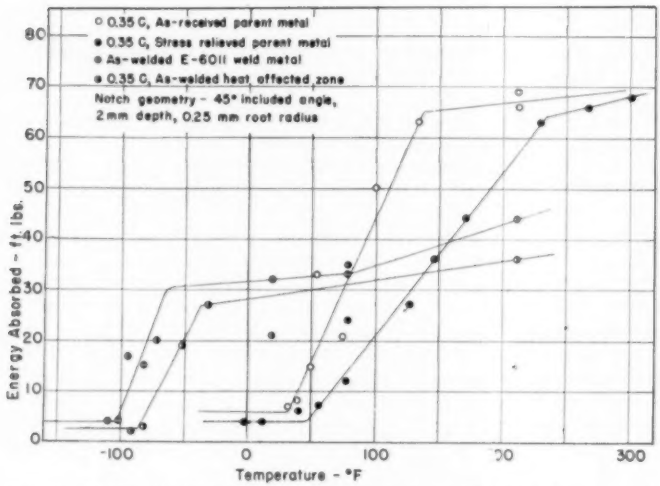

Fig. 10. Charpy V-notch transition temperature curves for 0.35% C parent metal and E-6011 weld metal

pressure cycles of the welded and stress-relieved cylinders (see Figs. 4 and 9). The yield strength of the weld metal in the as-welded condition is greater than that of stress-relieved weld metal (see Table 4).

Table 4—Tensile Properties of E-6011 Weld Metal

Condition	*Yield strength, psi*	*Tensile strength, psi*	*Elongation, % in 2 in.*
As-welded	52,000–60,000	65,000–70,000	22–30
Stress-relieved	47,000–54,000	60,000–68,000	28–36

Yielding of the weld metal in the as-welded cylinder during the second pressure cycle occurred at a pressure corresponding to the yield strength of the material. This indicates that the yielding which occurred during the first pressure cycle was essentially that required to relieve residual stresses and not sufficient to strengthen the material by cold working.

Pressure-strain data for the parent metal were obtained from cylinders Nos. 1 and 3 and the results were assumed to apply to the other cylinders tested. The absence of any yielding below a pressure corresponding to its yield strength in the parent metal 2.5 in. from the weld in both the as-welded and stress-relieved cylinders indicates that any residual stresses existing in that area were negligible and had little or no effect on the initial yielding of the material.

Thermal stress-relieving treatments are given to welds because it is a generally held opinion that the presence of residual stresses in a weldment will materially reduce the strength and ductility of the weldment. The fact that the thermal stress-relieving treatment will reduce the strength of the weldment is often ignored. The results of this investigation indicate that although there were residual stresses present in the weld metal of the as-welded cylinder they were relieved mechanically (mechanical stress relief as evidenced by yielding occurring at a lower pressure during the first pressure cycle in the as-welded cylinder than in the stress-relieved cylinder) during the first pressure cycle without reducing the ultimate strength of the cylinder. Failure of the as-welded cylinders during the second pressure cycle occurred at equivalent or greater pressures than did failure of the welded and stress-relieved cylinders.

Properties of Cylinders in Which Weld Metal and Parent Metal Are of Equivalent Composition and Strength

The cylinders in which the weld metal and parent metal were of comparable composition and strength were tested at a temperature of 75° F and under a circumferential to longitudinal stress ratio of 2:1.

A comparison of circumferential elongation of the cylinders from which the weld reinforcement had been removed by machining indicates that the conditions produced by welding (metallurgical properties and/or stress state) tend to resist the expansion due to internal pressures when the weld metal is of equivalent or greater tensile strength than the parent metal. In the welded and stress-relieved cylinder No. 1 in Fig. 11, the circumferential elongation at the weld was essentially the same as in the parent metal whereas in the as-welded cylinder, No. 3 in Fig 12, the circumferential elongation at the weld was significantly less than in the parent metal.

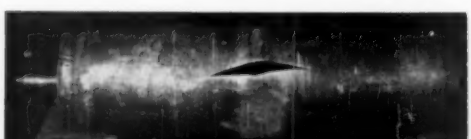

Fig. 11 Stress-relieved circumferentially welded cylinder (No. 1), tested at 75° F under a circumferential to longitudinal stress ratio of 2:1. Weld reinforcement removed

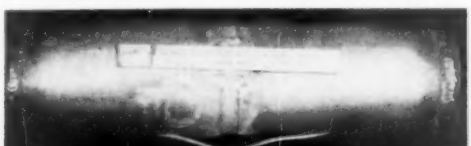

Fig. 12 As-welded circumferentially welded cylinder (No. 3), tested at 75° F under a circumferential to longitudinal stress ratio of 2:1. Weld reinforcement removed

Since it was assumed that mechanical stress relief occurred during the first pressure cycle in the testing of cylinder No. 3, the question arose as to whether this mechanical stress relief had any effect on the mechanical properties of the cylinder when subjected to the second pressure cycle. Cylinder No. 5, a duplicate of cylinder No. 3, was therefore tested to failure using only one pressure cycle. Yielding and failure (as measured by the pressure gage) occurred at approximately the same pressures for cylinders Nos. 3 and 5.

Although maximum loading had been obtained in cylinder No. 5, failure, caused by transverse cracking at a weld defect, was premature. For this reason full expansion and rupture did not occur as was the case in cylinder No. 3. The over-all appearance of cylinder No. 5, after testing, was similar to that of cylinder No. 3 except for the absence of the rupture in the former (see Fig. 12). Failure by rupture, in cylinders Nos. 1 and 3 (see Figs. 11 and 12) was initiated in a ductile manner and occurred in the center of the cylinder. Although higher pressures were applied to the cylinders tested

Fig. 13 Cylinder No. 2, same welding and testing conditions as cylinder No. 1 (Fig. 11) except that weld reinforcement was retained

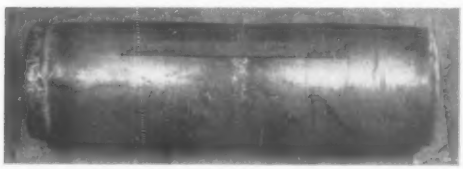

Fig. 14 Cylinder No. 4 same welding and testing conditions as cylinder No. 3 (Fig. 12) except that weld reinforcement was retained

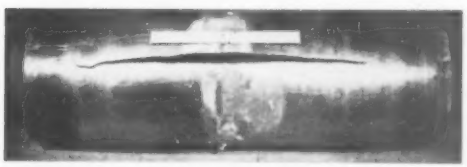

Fig. 15 As-welded circumferentially welded cylinder (No. 6), tested at 75° F under a circumferential to longitudinal stress ratio of 3:1. Weld reinforcement removed. Note ductile type of rupture

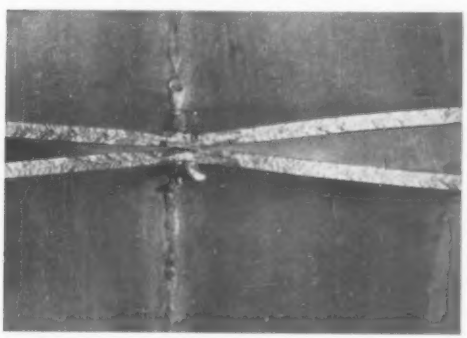

Fig. 16 Fractured surfaces of cylinder No. 6 (Fig. 15) showing initiation of fracture occurring in a ductile manner in the weld metal at a root defect

Fig. 17 As-welded circumferentially welded cylinder (No. 7) tested at −40° F under a circumferential to longitudinal stress ratio of 3:1. Weld reinforcement removed. Note brittle type of rupture

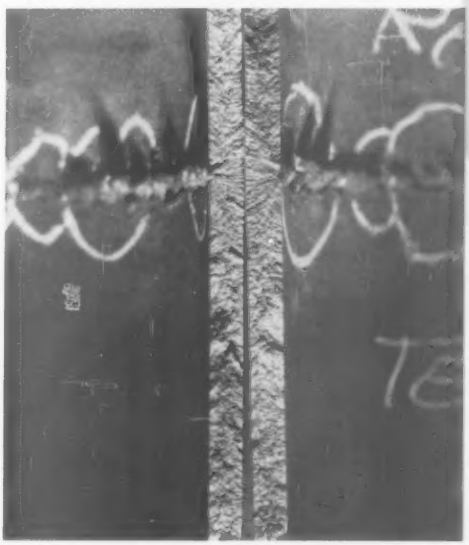

Fig. 18 Fractured surfaces of cylinder No. 7 (Fig. 17) showing initiation of fracture occurring in a brittle manner in the weld metal at a root defect

with the weld reinforcement retained, cylinders Nos. 2 and 4, than to the cylinders with the weld reinforcement removed, failure did not occur by rupture (see Figs. 13 and 14). Failure of cylinder No. 2 occurred by development of a pin hole tear in the central weld, whereas cylinder No. 4 developed a small crack in one of the cylinder head welds.

Properties of Cylinders in Which Parent Metal Was of Higher Strength Than the Weld Metal

The cylinders in which the parent metal was of higher strength than the weld metal (weld reinforcement was removed by machining from all cylinders in this group) were tested at room temperature and at $-40°$ F under restraint conditions and internal pressures which produced circumferential to longitudinal stress ratios of about 3:1 and 2:1.

The results indicate that the weld metal in either the as-welded or welded and stress-relieved cylinders had little or no influence on the expansion of the cylinders.

Stress-relieving had no apparent effect on the ductility and strength of circumferentially welded cylinders when tested at a temperatre of $-40°$ F and under a circumferential to longitudinal stress ratio of 2:1 (compare properties of cylinders Nos. 8 and 9 in Table 3). Increasing the circumferential to longitudinal stress ratio from 2:1 to 3:1 considerably decreased the ductility and strength in the as-welded cylinders when tested at a temperature of $-40°$ F (compare properties of cylinders Nos. 7 and 8). At a temperature of 75° F the increase in circumferential to longitudinal stress ratio from 2:1 to 3:1 decreased the strength of the as-welded cylinder but did not lower its ductility.

Fig. 19 As-welded circumferentially welded cylinder (No. 8) tested at $-40°$ F under a circumferential to longitudinal stress ratio of 2:1. Weld reinforcement removed. Note brittle type of rupture

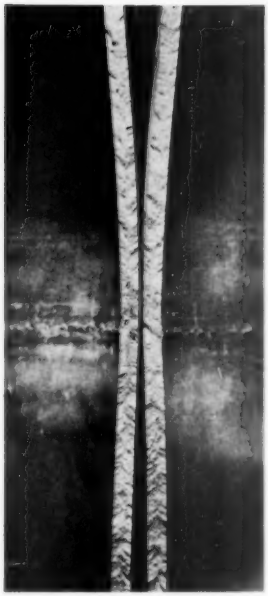

Fig. 20 Fractured surfaces of cylinder No. 8 (Fig. 19) showing initiation of fracture occurring in a brittle manner in the weld metal

All of the cylinders tested at $-40°$ F, with the exception of cylinder No. 9, ruptured in a brittle manner (see Figs. 15 through 20). The testing of cylinder No. 9 (welded and stress relieved) was not continued to rupture due to the fact that a leak had appeared in the pumping system after the maximum load had been reached. The cylinder tested at room temperature (cylinder No. 6) ruptured in a ductile manner Although rupture occurred in a brittle manner in those cylinders tested at $-40°$ F under a circumferential to longitudinal stress ratio of 2:1 the ductility (as measured by circumferential elongation and reduction in thickness) prior to rupture was surprisingly high.

The results of tests at $-40°$ F indicate that the parent metal notched bar impact properties are not indicative of the ductility of the material in a welded cylinder when the cylinder is loaded hydrostatically, producing a biaxial stress ratio of 2:1. The ductility of a cylinder welded under restraint and loaded hydrostatically, producing a biaxial stress ratio of 3:1, more nearly corresponds to the notched-bar transition impact properties.

CONCLUSIONS

The results obtained during this investigation can be summarized in the following conclusions.

1. Circumferentially welded mild steel cylinders in the as-welded condition have a somewhat higher unit stress to rupture with only slightly lower ductility (tested at 75 and −40° F under normal restraint) than do similar cylinders in the stress-relieved condition.

2. The decrease in ductility and unit stress to rupture due to a combination of low temperature (−40° F) and high longitudinal restraint, of circumferentially welded cylinders, was significantly greater than the decrease due to low temperature with normal restraint.

3. The removal of residual stresses by mechanical stress relief (yielding occurring at a pressure below that corresponding to the yield strength of the material) occurred in the weld metal of the as-welded cylinders during the first pressure cycle with no deterioration of subsequent mechanical properties.

4. When the weld metal and parent metal were of equivalent tensile strength the circumferential band of weld metal in the as-welded cylinder restrained the cylinder from expanding, under high internal pressure, to a greater degree than did the band of weld metal in the stress-relieved cylinder.

5. Removal of the weld reinforcement resulted in a decrease in the unit stress to yield and unit stress to rupture of the cylinders in both the as-welded and stress-relieved cylinders.

6. In cylinders in which the parent metal was of higher tensile strength than the weld metal, the weld metal in either the as-welded or stress-relieved cylinders had little or no influence on the strength and ductility of the cylinder.

ACKNOWLEDGMENTS

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Porosity in Mild Steel Weld Metal

(Continued from page 586-s)

troversial subject, not only with respect to porosity but also with respect to such defects as microfissures, underbead cracking and low-impact values generally attributed to hydrogen.

As a pioneer of the low-hydrogen electrode in this country, I might be expected to advocate its use to the exclusion of other types. While it has opened up new applications for covered electrodes making possible the welding of steels not weldable with conventional electrodes, it is my opinion that too much emphasis has been placed upon the potential adverse influence of hydrogen without due consideration to the application.

For those applications where hydrogen must be held to a low value, it is generally accepted that low-hydrogen electrodes are essential. On the other hand there are many uses for which conventional electrodes have been eminently satisfactory in the past and will continue to be in the future. The present consumption figures for mild steel and low-alloy electrodes show that conventional types outweigh low-hydrogen by a substantial margin. These figures will probably shift in favor of the low-hydrogen electrode with increasing use of alloy steels.

With further reference to the prevention of porosity, any attempt to generalize on the correct value for such procedure variables as welding current, arc voltage and travel speed serves more to confuse than to inform the reader confronted with welding problems. Factors which delay the onset of solidification frequently contribute to the deposition of sound welds but undue delay may also introduce porosity or increase its magnitude.

Adequate deoxidation, which is an important factor influencing soundness, is achieved not only by the addition of deoxidizers to an electrode coating, but also by pickup of base metal. The extent to which each of these sources of deoxidizers influence weld soundness is, therefore, a function of the electrode formulation, base metal composition and welding procedure. When dealing with the chemistry of deoxidation, it must be considered in terms of dynamic equilibrium rather than of static equilibrium in which case welding current, arc voltage, rate of travel and thermal capacity of the work become major factors.

The diverse applications of welding have placed a heavy burden on suppliers to provide materials capable of meeting the rigid requirements of industry. If a fabricator is confronted with a problem involving the selection of materials and/or procedure, it would be advisable for him to first attempt to work out a solution with the facilities of his own plant. In the event he is unable to find a satisfactory answer to his problem, he should then consult his suppliers for guidance to avoid costly and time-consuming delays.

(Authors' Reply on page 606-s)

Factors Which Determine the Performance of Aluminum Alloy Weldments

The Explosion Bulge Test is utilized to establish the factors which determine the performance of aluminum alloy weldments of the heat-treatable and magnesium solution hardening type

by W. R. Apblett, Jr., C. R. Felmley, and W. S. Pellini

Abstract

The Explosion Bulge Test has been utilized to establish the factors which determine the performance of aluminum alloy weldments. Two types of alloys were investigated: (1) the common heat-treatable type alloys (61S and 24S) and (2) the solid solution hardening type containing Mg as the primary alloying element. The deformation characteristics of the various weld and base metal combinations were determined by means of photogrids applied to the surface of the specimens. Tensile test data of the weld joints and parent plate were also obtained in order to establish the significance of the deformation characteristics.

It is indicated that the inability of weldments of heat-treatable type alloys to develop appreciable deformation is due to strain concentration in the low-strength weld and heat-affected zone. Weldments of the solid solution hardening type alloys are capable of developing extensive deformation prior to failure due to a more desirable distribution of strain in the weld, heat-affected zone and parent plate. This is the result of a closer matching of the strengths and ductilities of the various components of the weldment which is possible by the use of these alloys.

INTRODUCTION

THE use of heat-treatable type aluminum alloys in welded structures is desirable because of the high strength-to-weight ratios of these alloys. However, the low weld joint efficiencies which are obtained with even the best of present welding practice greatly reduce the effective strength-to-weight ratios and accordingly the advantage of using these alloys. This is true on any basis of design, for yield strength, tensile strength and elongation are all greatly reduced. The conventional 61S-T6 alloy welded with 43S provides an example. The base alloy develops a yield strength of 42,000 psi while the weld joint develops approximately 20,000 psi; thus, for any given safety factor, with the design based on the yield strength, it is necessary to increase the section size (hence weight) by approximately a factor of two. For alloys of higher yield strength than the 61S type (24S and 75S) the joint efficiency is much lower, in fact, at the present no advantage is to be gained by the use of alloys of yield strengths higher than 61S-T6 for welded fabrication.

It is well known that design cannot be based entirely on yield strengths even though healthy factors of safety are used to allow for points of stress concentration. The role of ductility must be considered also as a safeguard against unexpected or indeterminate stress conditions which may arise in service. In this respect the low ductilities shown by weldments of heat-treatable, high-strength aluminum alloys (61S, 24S, 75S) indicate that the margin of safety provided by ductility is not very great. For service entailing the possible development of extensive deformation, such as may be expected for certain types of military equipment, the ductility factor is of major importance. Inasmuch as this type of service may be considered to represent the limiting case, this investigation was directed at establishing the factors which determine the performance of weldments for such service by means of the Explosion Bulge Test. The factors which control performance are considered to apply equally to less rigorous service.

Previous investigations by the Naval Research Laboratory[1, 2] have indicated that metallurgical limitations preclude the development of adequate improvement in the welded joint efficiencies of high-strength aluminum alloys of the heat-treatable type, due to the low strengths of the cast weld material and of the weld heat-affected zone. The present outlook is that near-maximum joint efficiencies for fusion welds in these materials have been obtained. Tensile joint efficiencies in the order of 60–70% of the base metal tensile properties may be developed by the use of welds of higher strength such as 75S; however, the ductilities are extremely low due to localization of strain in the weld and heat-affected zone.

In Great Britain and Continental Europe, high-

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Table 1—Chemical Compositions

Alloy	*Mg*	*Si*	*Cr*	*Mn*	*Cu*	*Zn*	*Fe*	*Ti*
				Plate materials				
618-T6	1.0	0.6	0.25	0.15	0.25	0.20	0.70	
248-T4	1.5	0.5	0.10	0.60	4.5	0.10	0.50	
548-H34	3.21	0.06	0.20	0.04		0.08	..	
568-H34	5.33	0.06	0.10	0.10	0.01	0.06	..	
5% Mg (A)	5.10	0.14	0.02	0.28	0.02	0.10		
5% Mg (B)	5.15	0.22	..	0.40	0.23	..	0.46	0.044
				Electrode wires				
438	..	5.4	..	..	0.30	0.10	0.80	..
548	3.52	0.10	0.23	..	..	0.08	..	..
568	5.13	0.09	0.12	0.13	..	..	..	0.002
5% Mg	5.02	0.12	0.02	0.28	..	..	..	0.002
6% Mg	6.15	0.07	..	0.27	0.21	..	0.14	0.02
6.5% Mg	6.50	0.10	..	0.26	0.24	..	0.17	0.03

strength, solid solution alloys of the aluminum-magnesium (3–5%) type have been used for welding fabrication. These alloys develop tensile strengths as high as 40,000–45,000 psi in the annealed condition with concomittant ductilities of 25–30%. The relatively limited response to heat treatment shown by commercial alloys of this type may be a disadvantage for some purposes, since these alloys must be formed in the condition in which they are used in service. Therefore, if high strength of the finished structural parts is desired, it is necessary to choose a highly alloyed material of difficult formability. This is not the case with the age-hardening type of alloys which may be worked in the annealed or solution treated temper and subsequently heat treated to give high mechanical properties. The relative insensitivity of the Al-Mg alloys to heat treatment serves as an advantage in the case of welding inasmuch as the thermal effects of welding do not result in the development of a heat-affected zone of extremely low strength as in the case of heat-treatable alloys.

MATERIALS AND WELDING PROCEDURES

The chemical composition of the plate material and electrode wires investigated are presented in Table 1. The 618 and 248 plates were produced on normal production schedules; the 548 and 568 plates were produced commercially on an experimental basis. The 5% Mg (A) and 5% Mg (B) plates are commercial British alloys, produced to the same general specification by different manufacturers. The majority of the tests were conducted on plates of 1/2 in.-thickness although several tests involved 3/4-in. plates. The tensile properties of these materials in the as-received condition are summarized in Table 2. These values were obtained by testing standard ASTM 0.357-in. tensile bars cut from the materials with the longitudinal axis of the specimen in the direction of rolling. These data indicate that the tensile strengths of the Al-Mg alloys are comparable to the 618-T6 alloy. The yield strengths of the British Al-Mg alloys are considerably lower than the values shown by the 618-T6 alloy and somewhat lower than the values shown by the 548 and 568 alloys, due to the differences in the amount of cold working. The exact temper of the British alloys is unknown; the 548 and 568 are in the half-hard condition. The elongations of all of the alloys are of high order; however, the values developed by the British Al-Mg alloys are superior to the 548 and 568 types.

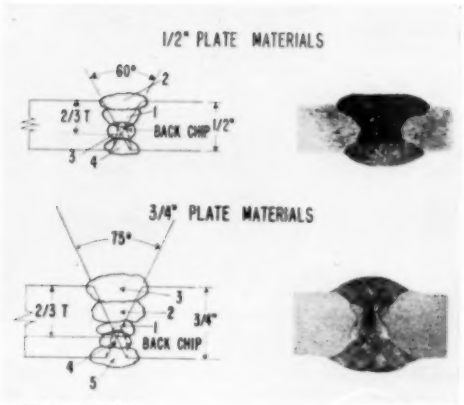
Table 2—Tensile Properties of the Plate Materials

Alloy, 1/2 in. thick	*Yield strength, 0.2% offset, psi*	*Tensile strength, psi*	*Elongation, % in 2 in.*	*Reduction in area, %*
618-T6	41,600	44,300	12.5	50.5
248-T4	53,200	69,800	19.0	24.0
548-H34	32,600	42,600	12.5	26.5
568-H34	34,000	49,500	18.5	43.0
5% Mg (A)	28,800	43,600	23.0	50.5
5% Mg (B)	28,600	43,300	23.0	42.0

The 438, 548 and 568 electrode wires were received with a chemically cleaned and stabilized surface. The 5% Mg wire was a British product and was received in the as-drawn condition. It was necessary to chemically clean this wire prior to welding to produce sound welds.

The test weldments were prepared under fully automatic welding control in order to hold conditions as constant as possible. The welding procedure outlined in Fig. 1 was used for all weldments with the exception of the two weldments made with 6 and 6.5% Mg electrode wire. These latter weldments were obtained from a commercial source and were made manually with 3/32-in. electrode wire using slightly higher amperage but otherwise essentially the same welding technique. An Esterline-Angus recording voltmeter and ammeter were used to record the arc conditions during the preparation of the test weldments. The test plates were prepared by welding two plates, 9 x 18 in. x plate thickness, the weld being perpendicular to the direction of rolling in all cases.

After welding the weld reinforcement was removed flush with the surface of the base metal. All welds were radiographed and the presence of porosity, if any, was recorded for possible correlation with performance. The flat surface of the explosion bulge test plates was then polished and printed with a 50-line-per-inch photogrid. Details of the application of the photogrid were presented in an earlier report.[3]



Welding Conditions

All welding was performed with an automatic inert-gas-shielded metal-arc welder in the downhand position with a backhand welding technique. All electrode wires were 1/16 in. in diameter.

	1/2-in. plate	3/4-in. plate
Current, amp	245–255	255–275
Voltage	23–25	24–26
Argon, cu ft/hr	50	50
Travel speed, in./min.	10–15	8–12
Wire feed rate, in./min	160–250	200–275

Note 1: 15 in./min. on first and third passes and 10 in./min. with weave on second and fourth passes.
Note 2: 12 in./min. on first, second and fourth passes and 8 in./min. with weave on third and fifth passes.

Fig. 1 Butt joint design

TENSION TESTS

As part of the problem of determining the engineering significance of the various performance characteristics shown by the bulge tests, information is required concerning the conventional tensile properties of the weldments. The tensile test data presented in Table 3 and Fig. 2 were obtained using standard ASTM specimens of full plate thickness (1/2 in.) having a 1 1/2-in. wide reduced section and an 8-in. gage length. The center-line of the weld was located at the midpoint of the gage length and transverse to the direction of loading. The tensile strengths of the 61S-T6 alloy weldments are fixed by the strength of the heat-affected zone since failure occurs in this zone regardless of the electrode wire employed. The yield strengths and elongations of these weldments vary somewhat, depending on the relative flow strengths of the HAZ and weld metal. The 24S-T4 weldments have relatively high-tensile strengths accompanied by extremely low elongation values. The tensile strengths and elongation values of the Al-Mg (5%) alloys are considerably higher than the 61S-T6 weldments.

The property most commonly referred to for engineering design of structures is the yield strength; for aluminum alloys this is determined by the 0.2% offset method. For materials which strain uniformly such a figure has considerable engineering significance; however, in the case of aluminum alloy weld joints, particularly the heat-treatable type, the value is deceptive in that the strain occurs in very narrow regions (the overaged zones) of the over-all gage length. Accordingly, the numerical values are dependent on the gage length used in computing the 0.2% offset. For an 8-in. gage length the numerical value of the yield strength will be considerably higher than that obtained by using a 2-in. gage length due to the fact that the flow strength curve of the small volume of material

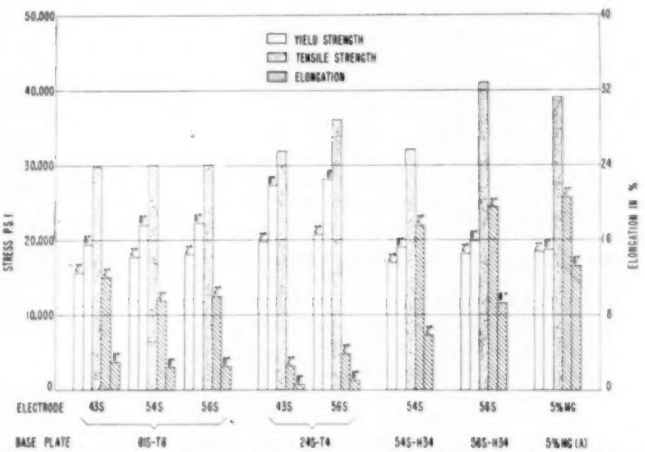


Fig. 2 Comparison of tensile properties of welded 61S-T6, 24S-T4, 54S-H34, 56S-H34 and 5% Mg alloys

Table 3—Tensile Data for Composite Weld Specimens

Base metal, 1/2 in. thick	*Weld*	*Yield strength, psi, 0.2% offset 2 in.*	*Yield strength, psi, 0.2% offset 8 in.*	*Tensile strength psi*	*Elongation— % in 2 in.*	*% in 8 in.*
61S-T6	43S	15,600	19,300	29,800*	12.0	3.0
61S-T6	54S	17,800	22,000	30,000*	9.5	2.4
61S-T6	56S	18,100	22,200	30,000*	10.0	2.5
24S-T4	43S	19,800	27,300	31,800†	2.5	0.5
24S-T4	56S	20,700	28,300	36,200†	3.8	1.0
54S-H34	54S	16,800	18,900	32,100†	17.5	5.8
56S-H34	56S	18,200	19,800	41,000†	19.5	9.2
5% Mg (A)	5% Mg	18,300	18,500	39,800†	20.5	14.0

NOTE: All data are the average of three specimens.
* Heat-affected zone failure.
† Weld metal failure.

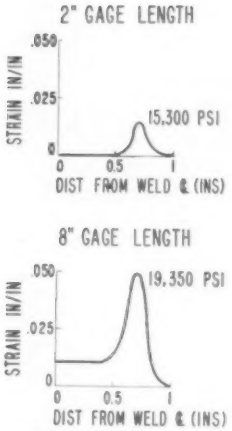


Fig. 3 Strain plots for 0.2% offset yield strength

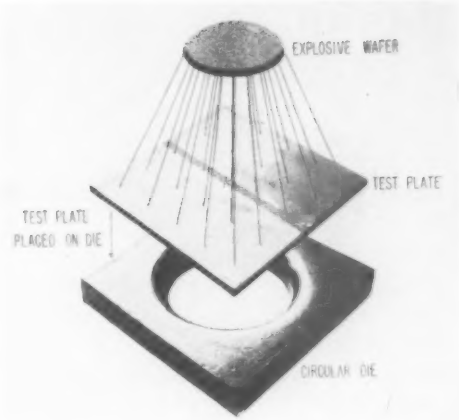


Fig. 4 Schematic representation of the Bulge Test

that is actually straining determines the flow strength of the weldment. In terms of unit strain this means that the narrow region undergoing extension must strain more (hence support a larger load) before reaching the 0.2% value of the longer gage length. Figure 3 presents strain plots obtained from the photogrid applied to a tensile specimen of 61S-T6 welded with 43S electrode wire. The 0.2% offset yield strength for a 2-in. gage length is 15,300 psi and represents unit strains of approximately 1.5% in the overaged zone. The 0.2% offset yield strength for an 8-in. gage length is 19,300 psi and represents strains of nearly 5% in the overaged zone and approximately 1% in the weld.

These data indicate that design based on yield points determined by the usual tensile tests of weld joints may be in considerable error. In many cases such design will be based not on loads which result in an assumed 0.2% elongation of the critical position but on loads which are actually developing *5% or more* of deformation in the "weak link" region. The importance of the *ductility* aspects of weld joint properties accordingly is accentuated. The relative differences between the 2- and 8-in. gage length properties shown in Fig. 3 reflect the relative tendencies for strain localizations in narrow "weak link" regions. Accordingly, the slight differences in yield strength due to gage length shown by the 5% Mg (A) weldments indicate that general deformation has occurred over the entire gage length.

EXPLOSION BULGE TESTS

The Explosion Bulge Test[4,5] developed by the Naval Research Laboratory provides a convenient means of investigating the performance of weldments in thick aluminum plate. Basically, the Explosion Bulge Test method represents a procedure of testing rather than a "test." The method is designed for the investigation of structural performance on a model scale. The model structure is represented by a full thickness plate or weldment which is loaded by the application of gas pressure supplied by the detonation of an explosive, while supported by a die having a chamfered circular opening. The gas pressure clamps the plate to the die support and produces controlled bulge deformation over the unsupported area. Bulging over a circular die results in balanced biaxial tension stresses in the test material; Fig. 4 illustrates the equipment and method.

The explosive is used in a manner such that the *effect* of the energy delivered by the explosive rather than the explosive charge weight serves as the criterion of performance. The criterion of performance used in this investigation was the amount of deformation developed in the plate material near the weld. The explosive charge is placed at a distance such that a small bulge increment is developed on each of several shots. The standard charge used is 4 lbs. of pentolite precast to a wafer shape as shown in Fig. 4. This charge permits development of bulges in small increments for a variety of test materials. Weldments of mild steel of $^3/_4$- and 1-in. plate thickness may be tested by using an offset distance of 15–24-in. Aluminum weldments of $^3/_4$ and 1-in. thickness require an offset distance of 36-in. which is approximately the limiting distance which may be used conveniently. Aluminum weldments of $^1/_2$-in. thick plate require a 2-lb charge used at offset distances of 24–36 in.

Inasmuch as the development of a failure results in the immediate termination of straining in the direction perpendicular to failure, it is possible to determine the exact level of strain reached at the time of failure by measurement along the weld perpendicular direction. This technique requires the use of small increments of bulge deformation since this prevents extensive tearing of the bulge which would result from a single, high energy blow. This method relegates the use of the explosive to the simple function of providing the energy required to evolve the bulge in small steps. Accordingly, the specific explosive weight and offsets which

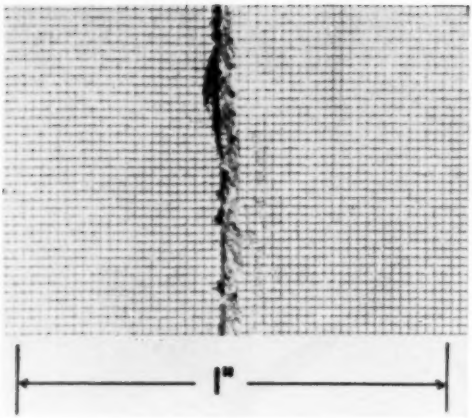


Fig. 5 Typical grid application

are used in the various tests are of no significance to the bulge test data which are presented.

Strains were determined by measuring the distortion of the 50-line-to-the-inch grid applied to the test plate. An enlarged photograph of this grid is shown in Fig. 5. The inherent error in the spacing of the grid lines is approximately ±0.00005 in. The use of a microscope mounted on a micrometer slide as a means of measuring the distortion of the grid permits measurement of strains as low as 1%.

Figure 6 presents strain data illustrating the increment bulging of a 5% Mg prime plate. These data illustrate an important mechanical feature of the bulge test. It is observed that the strain level of a region approximately 3 in. in diameter is nearly uniform to approximately 14% strain. This represents the limit of uniform biaxial tension, for this material, necking follows as indicated by the peak strain at the center of the bulge. The development of the neck-down region is as expected for any tensile test. The 3-in. diam region of uniform extension represents a portion of the bulge of sufficient size to eliminate steep radial strain gradients in the region to be occupied by the weld, the heat-affected zone and a portion of the prime plate. Thus, all three regions are tested under conditions of equal load mechanics. If the bulge were made somewhat smaller than the 15-in. diam bulge employed, the 3-in. diam region would shrink in proportion, preventing equal biaxial loading of the three test regions of interest. The bulge size was not arrived at arbitrarily but was based on meeting these desired conditions. It may be concluded that the method of testing does not discriminate against the weld zone. This is important for any fundamental study of the plastic interactions of weld, HAZ and prime plate required for an analysis of the factors which determine performance.

The bulge data which are to be presented represent the average of at least two and generally three test plates for each base metal-weld metal combination, except for the weldments which were supplied commercially in which case only one weldment was tested. Reproducibility in the several weldments tested for each base metal-weld metal combination was good; the maximum deviation from the average strain level plotted was approximately 10% of the unit strain value.

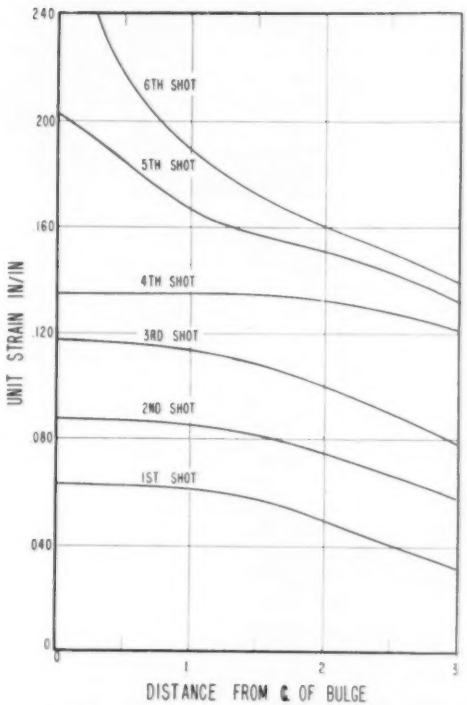


Fig. 6 Strain data illustrating the increment bulging of a 5% Mg prime plate

Performance of Prime Plates

Figure 7 illustrates the extreme deformation which may be obtained without failure in the case of the prime plates. The strain distributions obtained from the photogrid have been superimposed on the photographs of the various bulges. These materials were not forced to ultimate failure since this information was not of particular consequence to the investigation. The level of deformation was varied to illustrate variations in strain distribution in relation to the bulge geometry. The 24S-T, 54S and 56S still indicate uniform strain in the pole region while the 61S-T and 5% Mg (A) show necking, the necking being considerable in the case of the 5% Mg (A). It may be observed that with the development of necking the true spherical shape is lost and a secondary bulge develops in the pole region.

Performance of 61S-T6 Weldments

The most commonly used material for high-strength structural applications involving welding is the 61S-T6

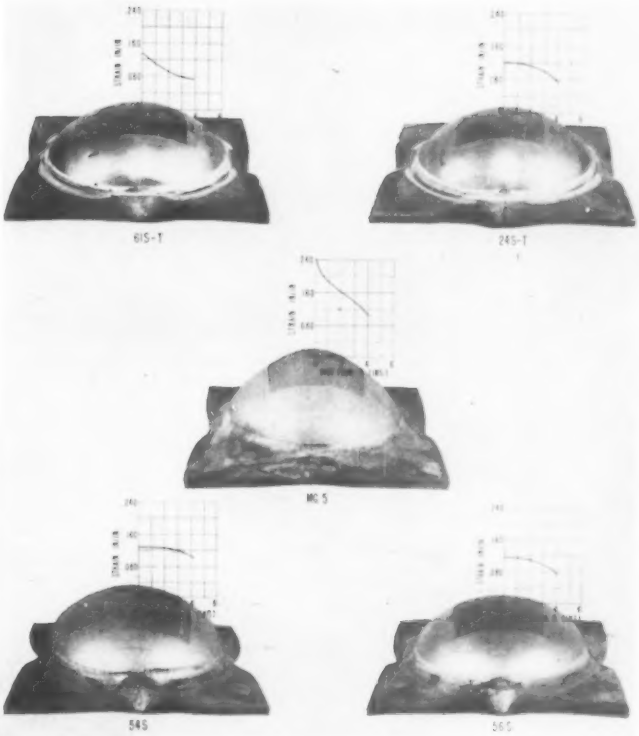


Fig. 7 Prime plate bulges

aluminum alloy; the electrode normally specified for welding this material is 43S. Inasmuch as the weld deposit is of lower strength and ductility than the plate material this combination fails in the weld at a very low level of deformation, as illustrated in Fig. 8. The use of 54S electrode results in essentially the same performance. A moderate improvement in performance is possible by the use of 5% Mg and 56S electrode wires which have higher strength and serve to transfer a portion of the weld strain to the more ductile HAZ and base plate (56S only) as illustrated in Fig. 9. However, the improvement in performance is relatively moderate because of the continued high concentration of strain to a narrow region.

Performance of 61S-O and 2S Weldments

If the 61S-T6 base metal is heat treated to lower its strength in relation to that of the deposited weld metal i.e., fully annealed 61S plate material welded with 43S electrode wire, an entirely different type of performance is noted, as shown in the upper bulge of Fig. 10.* The lower bulge* illustrates similar performance of 2S base material welded with 43S. Inasmuch as the strength level of the weld in these two cases greatly exceeds the strength of the plate, the level of strain in the weld is now quite low in relation to the strain in the base material and the weldment can undergo considerable deformation prior to failure. These weldments are not suited for high-strength structural applications due to the low strength of the plate material. However, the comparison clearly illustrates that the principal element which determines the poor performance of 43S filler in 61S-T6 is the strain concentration factor.

Performance of 24S-T4 Weldments

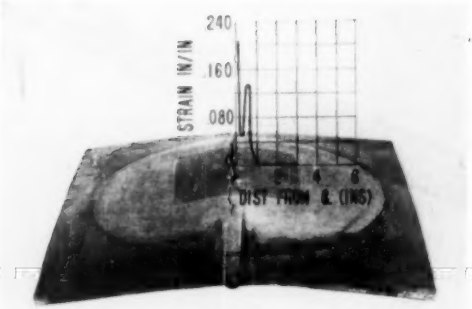
Figure 11 illustrates the extremely poor performance exhibited by weldments in 24S-T4 base material. This is the result of the extreme concentration of strain to the low-strength welds. The absence of strain in the heat-affected zone indicates that the yield strength of this region is greater than the ultimate strength of the deposited weld metal.

Performance of Al-Mg Weldments

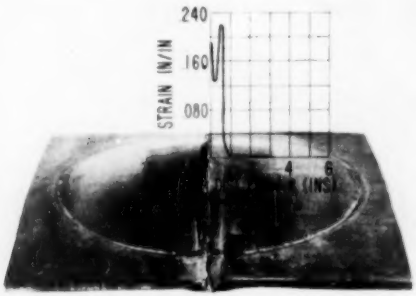
At the onset of this investigation the British alloys containing approximately 5% Mg were the only plate materials of this type commercially available having tensile strength properties similar to those of the 61S-T6 alloy. Figure 12 illustrates the excellent performance of 5% Mg (A) material when welded with 3.5% Mg (54S) wire and with 5% Mg (British) wire. The 3.5% Mg weld has lower flow strength than the 5% Mg (A) plate as indicated by the severe strain concentration in the weld which leads to ultimate failure. The excellent ductility of the weld deposit, however, prevents failure until the plate has been forced to develop extensive deformation. The weldment featuring the 5% Mg filler performs in essentially the same fashion except that the level of deformation developed in the weld is considerably lower as expected from the higher strength developed by the 5% Mg composition. The strain plots also indicate that there is a strain deconcentration just at the fusion line indicating higher flow strength for this region.

In an attempt to improve the performance by decreasing the strain concentration developed in the weld several weldments were tested which were prepared

* NOTE: These two figures represent the only case in which the weldments were not bulged *to failure*. All other strain plots which are presented represent the strain conditions at failure.

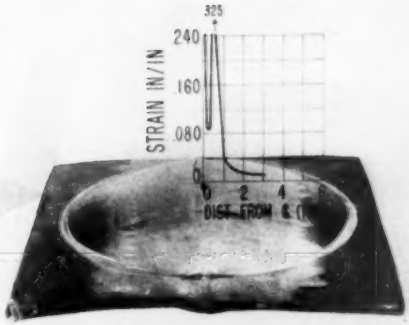


61S-T6 WELDED WITH 43S ELECTRODE

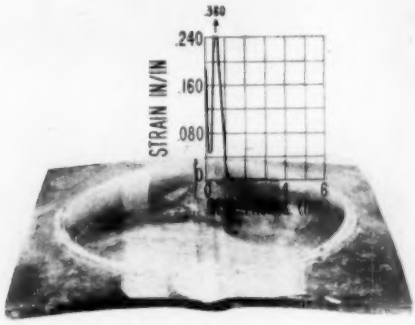


61S-T6 WELDED WITH 54S ELECTRODE

Figure 8



61S-T6 WELDED WITH 56S ELECTRODE



61S-T6 WELDED WITH 5% MG ELECTRODE

Figure 9

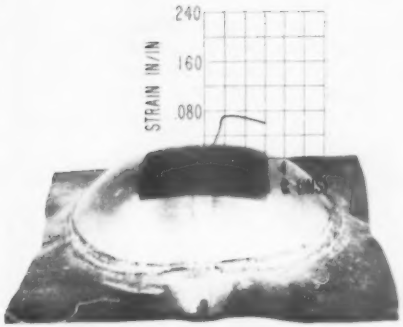
from more highly alloyed electrode wires. These weldments were obtained from a commercial source and the base alloy used was produced by a different manufacturer to the same British specification. This alloy has been designated 5% Mg (B) in the table of chemical analysis. When these weldments were tested the higher strength of the cast weld resulted in a strain deconcentration in the weld and a severe localization of strain in the narrow annealed region adjacent to the weld, as illustrated in Fig. 13. Failure was thus forced into this annealed region and a general decrease in performance was noted. It had been expected that the use of an overmatching weld would result in improved performance of the weldments since no severe strain concentrations were noted in the annealed regions of the 5% Mg (A) weldments prepared with 3.5 and 5% Mg wire. It was apparent that the decrease in performance was associated with a radically different response of the two base materials (A and B) to the annealing cycle of the weld. This fact was substantiated by testing 0.357-in. tensile bars which had been annealed for ½ hr at 1000° F. As suspected, the decrease in tensile strength properties on annealing was considerably greater for the "B" alloy. The following table summarized the effect of the annealing treatment on the tensile properties.

Table 4

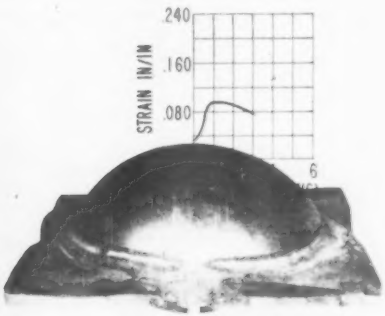
	5% Mg (A)		5% Mg (B)	
	As-received	Annealed	As-received	Annealed
Yield strength, psi	28,800	14,600	28,600	14,400
Tensile strength, psi.	43,600	40,300	43,300	33,200
Elongation, % in 2 in.	23	27	23	28

It should be noted that the large decrease in the tensile strength of the 5% Mg (B) alloy is not due entirely to elimination of cold work since aging this alloy after annealing for 1 hr at 250° F increases the tensile strength to 40,000 psi; the 5% Mg (A) annealed alloy did not respond to aging treatments. The 5% Mg (B) alloy therefore is not representative of the strictly nonaging type of Al-Mg alloys. The difference in response to aging treatments may be ascribed principally to the higher copper content (0.23% as compared to 0.02%) of the B alloy.

It has been stated previously that the 54S and 56S alloys in plate form are being produced on an experimental basis in this country. These alloys are com-



61S-O WELDED WITH 43S ELECTRODE



2S WELDED WITH 43S ELECTRODE

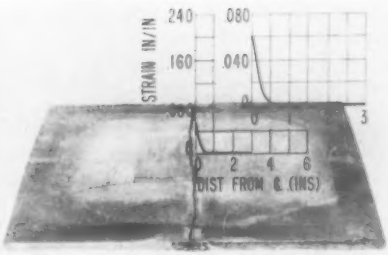
Figure 10

parable to the British Al-Mg type and therefore their performance is similar as shown in Fig. 14. The response of these alloys to the aforementioned annealing treatment ($^1/_2$ hr at 1000° F) is summarized in the following table:

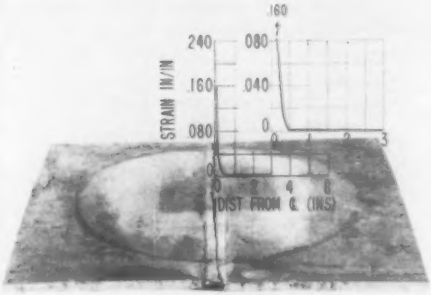
Table 5

	54S		56S	
	As-received	*Annealed*	*As-received*	*Annealed*
Yield strength, psi	32,600	12,700	34,000	15,700
Tensile strength, psi	42,600	34,600	49,500	44,000
Elongation, % in 2 in.	12.5	28	18.5	29

Since these materials were supplied in the H-34 temper (half-hard), the strength of the as-deposited 54S and 56S wire is lower than the base metal, and accordingly strain localization occurs in the weld leading to a weld failure. The annealed zone adjacent to the weld also develops a severe localization of strain, but failure does not occur at this position; this, it is felt, is



24S-T4 WELDED WITH 43S ELECTRODE



24S-T4 WELDED WITH 56S ELECTRODE

Figure 11

due to the higher ductility of the rolled material. The performance of the 56S-56S weldment is somewhat better than that of the 54S-54S weldment as indicated by a higher level of strain in the base plate.

In the foregoing sections of this report it was reported that solid solution-hardening alloys are not prone to develop a softened heat-affected zone of the type common to the age-hardening alloys. However, it is common practice to produce the solid-solution hardening materials in a work-hardened condition, frequently about the half-hard temper. Accordingly, when these materials are welded, the heat of the welding operation produces an annealed zone adjacent to the weld which is comparable in kind, although not in degree, to the overaged-zone of the heat-treatable alloys; this zone, therefore, develops marked strain concentrations. Alloy adjustments, aimed at developing moderate increases in tensile properties by age hardening, accentuate the annealed zone problem of the cold-worked alloys.

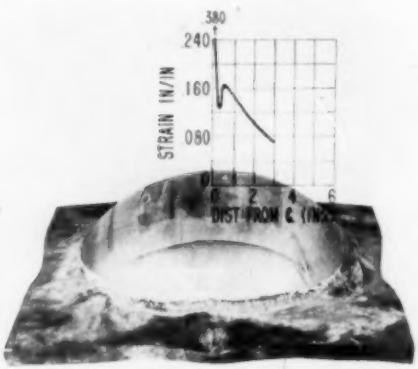
Effect of Weld Porosity on Performance

Inert-gas-shielded metal-arc welds may contain scattered porosity. A study of this porosity[6] indicates that it is mainly due to hydrogen, with the electrode wire being the main contributor of the gas. Such porosity is eliminated by the use of suitably cleaned

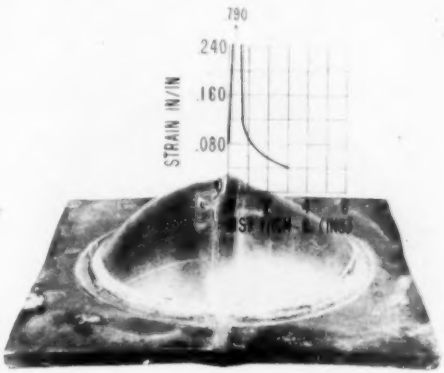
wire and by proper welding procedures. Most of the weldments which were prepared were found to be completely free of porosity; the various tests which have been reported thus far represent completely sound welds. The weldments which were found to contain small traces of porosity were supplemented by additional weldments which were made to contain porosity, and these were tested separately. The presence of isolated spots of porosity did not result in any consistent trend of decreased performance. Occasionally the performance would be seriously impaired and at other times only very minor changes would be observed for what appeared to be porosity of the same order of magnitude. Representative weldments are shown in Fig. 15; the amount and distribution of the porosity are shown in the X-rays included in the figure. Except for the particular area indicated the welds were free of porosity. It was also observed that significantly premature weld failure caused by localization of porosity occurs only in welds that have flow strengths lower than the base material. If the weld is of considerably higher flow strength than the base metal the effect of porosity is negligible. For example, a porosity count on a 43S deposit of a 2S weldment which performed as well as the 2S weldment shown in Fig. 10 showed a porosity count of 15 spots per inch having a size of $\frac{1}{16}$ in. in diameter.

Relative Performance of the Weldments

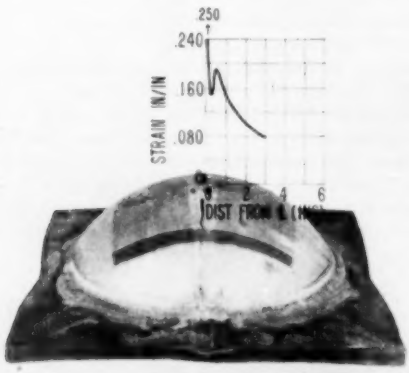
The foregoing discussions have been concerned with details of factors which determine the performance of aluminum alloy weldments. The performance level was based on a criterion of bulge deformation; i.e., the extent of general deformation developed prior to failure. The extent of deformation developed in the prime plate outside of the influence of the weld serves as a convenient performance parameter inasmuch as the bulge size is primarily a function of the deformation developed by the element which comprises the major portion of the



5%MG WELDED WITH 3.5% MG (54S) ELECTRODE

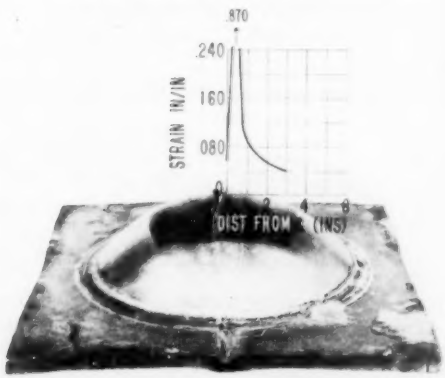


5% MG WELDED WITH 6% MG ELECTRODE



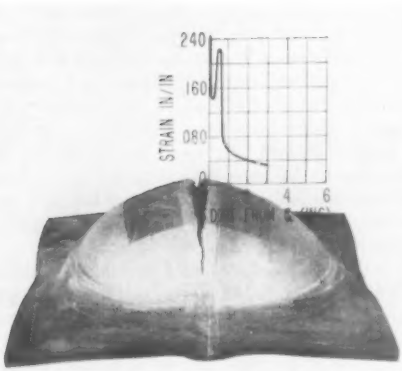
5% MG WELDED WITH 5% MG ELECTRODE

Figure 12

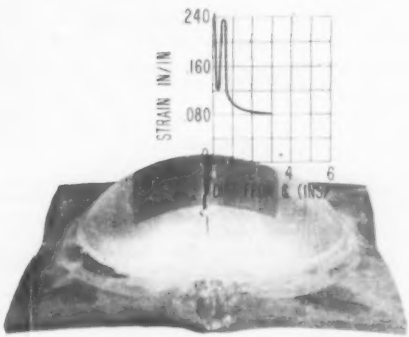


5% MG WELDED WITH 6.5%MG ELECTRODE

Figure 13

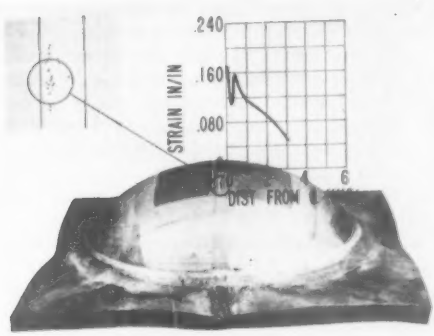


54S WELDED WITH 54S ELECTRODE

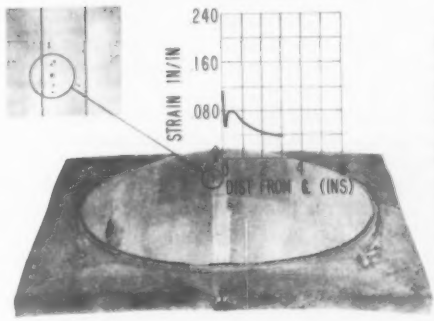


56S WELDED WITH 56S ELECTRODE

Figure 14



5% MG WELDED WITH 5% MG ELECTRODE



5% MG WELDED WITH 5% MG ELECTRODE

Fig. 15 Effect of localized porosity on performance

test area. It was demonstrated that nonductile behavior (fracture without bulging) was related to the inability of the weld joint to transfer or "shed" strain to the parent metal and also, that the various degrees of ductile behavior (fracture after the development of various degrees of bulge) were related to the degree of cooperative straining of the weld, HAZ and base metal. Accordingly, the level of strain developed by the plate at a fixed position in the "pole" region of the bulge may be taken as a convenient parameter of the relative performance of the various weldments.

Table 6 summarizes the level of strain developed at a position *in the plate* 1½ in. from the centerline of the weld.

Table 6

61-43, 0%	24-43, 0%	54-54, 5%	5 Mg-3.5 Mg, 11%
61-54, 0%	24-56, 0%	56-56, 9.5%	5 Mg-5 Mg, 12%
61-56, 2.2%			5 Mg-6 Mg, 7%
61-5% Mg,0%			5 Mg-6.5 Mg, 7%

It is noted that the weldments of heat-treatable alloys (61S and 24S) do not develop measurable deformation in the plate, except in the case of 61-56 combination. The performance of the Al-Mg alloy weldments is distinctly superior in this respect. The highest level of performance is demonstrated by the 5% Mg alloys of the nonaging type.

Summary Discussion and Conclusions

The results of this investigation indicate that the performance of aluminum alloy weldments is determined by the relative flow strengths and terminal ductilities of the weld and heat-affected zones as compared to the base plate. The poor performance of the high-strength alloys of the precipitation-hardening type is inherent to the localization of strain in either the weld or the HAZ resulting from the low flow strength and ductility of these zones. Increasing the weld reinforcement or using lap-plate reinforcement cannot be expected to provide a solution inasmuch as the low-strength, overaged HAZ remains to provide a detrimental strain concentration. The highly localized strain which is developed in either the weld or HAZ, de-

pending on the materials and the structural geometry of the weldment, effectively reduces the energy absorption prior to failure to relatively low values. Accordingly, it is necessary to rely on design which ensures that the yield point of the weakened zone in the weldment is not exceeded, since the margin of safety provided by the plastic yielding of highly localized regions is relatively small. Actually, the present design limits based on 0.2% yield strength probably do not ensure that the yield point of the critical zone is not exceeded. It has been demonstrated for the case of the 61S-T6-43S weldment that the 0.2% yield strength (20,000 psi) determined on an 8-in. gage length actually represents a strain of approximately 5% in the heat-affected zone and 1% in the weld, and that at a stress of 75% of the yield strength value (15,000 psi) the level of strain in the HAZ is approximately 1.5%. It is probable in this case that significant strain is developed at stress levels which approach 50% of the yield strength of the weldment as determined by conventional procedures. Thus, plastic flow is likely in the weldment at stress levels which are only 25% of the yield strength of the base plate (40,000 psi).

The use of solid solution type alloys appears to be promising. While these alloys are characterized by lower tensile and yield strength properties than the age-hardening type, it appears feasible to use weld metals which match the plate properties and accordingly approach 100% joint efficiency. From a standpoint of yield strength properties such weldments essentially match the conventional 61S-T-43S combination. It may be deduced from this fact that no advantage is offered in so far as design based on yield strength is concerned; however, such a conclusion neglects the high ability to absorb energy without failure which is inherent to the solid solution type. In effect this means that a large reserve of ductility is available to ensure the safety of the structure, as indicated by the extensive deformation which these alloys may endure prior to fracture. Such a reserve is not available to the 61S-T-43S type as indicated by the failure to develop bulge deformation. An improvement in the 61S-T type in this respect is indicated by the use of 56S filler metal; however, the level of performance remains distinctly inferior to the Al-Mg type.

It is demonstrated that cold working or partial age hardening of the Al-Mg alloys to increase the tensile properties is a distinct detriment to the performance of the weldments. The additional strength is obtained at the expense of reintroducing the difficulties associated with the heat-treatable alloys and prevents the use of filler metal of optimum strength. For the utmost performance, particularly in case of service which requires that the weldment develop extensive deformation prior to failure, it is essential that Al-Mg alloys be specified to have a minimum of cold work and aging effects.

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Porosity in Mild Steel Weld Metal

(Continued from page 595-s)

Authors' Reply

The authors wish to thank Mr. Mathias for his practical comments concerning metallic arc welding with coated electrodes. It should be pointed out that one of the main purposes of this literature review was to provide a background for the subsequent presentation by the authors of actual experimental data concerned with porosity in mild steel weld metal. It was not intended that such a review be used as a basis for the selection of welding electrodes or welding conditions for practical applications.

Column Strength Under Combined Bending and Thrust

Progress Report No. 6 on Welded Continuous Frames and Their Components

An analytical and experimental study of the strength of rolled structural shapes subjected to various combinations of axial load and end bending moment

by Robert L. Ketter, Lynn S. Beedle and B. G. Johnston

Abstract

Presented in this paper are the results of an analytical and experimental study to determine the strength of columns subjected to various combinations of axial load and end bending moment and to determine the behavior of such columns in the elastic and inelastic ranges. In this report emphasis has been given to yielding as the criterion of strength; however, a collapse solution based on the "simple plastic" theory is also included together with an approximate buckling solution based on the work by Jezek. In the experimental phase of the investigation tests were made on as-delivered rolled structural sections.

I. INTRODUCTION

IN 1945, the Welding Research Council through its Structural Steel Committee resumed its project at Lehigh University, directing that emphasis be given to fully continuous welded frame construction. As T. R. Higgins of the AISC has stated, "It was realized that little progress could be made in studying the effect of welds upon the strength of continuous frames until more was known about the effects of other variables present in these frames which might be of a magnitude to blanket completely, or considerably disturb, any effect due to welding." Thus, each component part of continuous frames is being studied to determine its behavior under load. This paper is the sixth in a series describing such studies and is concerned with the strength of steel columns of the type found in tier buildings and industrial construction.

In 1946, the American Institute of Steel Construction commenced sponsorship of the current investigation of columns loaded with combined axial force and end moment. The purposes were twofold: (*a*) to determine the plastic behavior of steel WF columns of the type used in building frames with continuous connections, and (*b*) to determine carry-over and stiffness factors. This program is now part of the larger investigation mentioned earlier to which the American Institute of Steel Construction now gives its support.*

Although the eventual objective is to develop practical design methods based on elastic and inelastic action, the first phase is to determine the elastic and plastic behavior of columns as influenced by variables such as condition of end moment loading, slenderness ratio, the ratio of axial load to the critical axial load, and size and shape of cross section.

More specifically, for the present program, the objective is to determine the influence on column strength of each basic form of loading and to relate it to existing specifications such as the AISC interaction formula, thereby indicating the true load factor implied in present design practice.

In order that the results of this investigation be more closely coordinated with findings at other institutions studying the behavior of compression members, the Column Research Council of the Engineering Foundation participates as a sponsor in an advisory capacity since one of the objectives of that organization is to guide column research and to study the application of the results to the design of compression members.† For example, at Brown University a project is under way at this time to study specifically the AISC Interaction Formula

$$\frac{f_a}{F_a} + \frac{f_b}{F_b} \leq 1$$

An unpublished report has been prepared.[21]

In a previous report[2] test apparatus used in the experimental part of the Lehigh investigation was described. It also included an analysis which showed that the development of restraint in the ends of columns bent in single curvature in tier-type frames would require that the adjoining beams carry greater than simple beam bending moments at mid-span.

Since current specifications for civil engineering structures are in general based on initial yielding, emphasis has been given here to this criterion of strength. It is the purpose of this report to present equations for determining the initial yield strength of a column subjected to various loading conditions and to compare theory with the results of tests of full size structural steel columns. Equations for determining the ultimate or collapse strength of columns under certain conditions of loading are presented according to the simple plastic theory. The corresponding collapse strengths of the columns tested are also indicated.

Robert L. Ketter is Research Instructor and **Lynn S. Beedle** is Assistant to the Director at the Fritz Engineering Laboratory, Lehigh University. **Bruce G. Johnston** was formerly Director of Fritz Engineering Laboratory; now Professor of Structural Engineering, University of Michigan.

This work has been carried out as a part of an investigation sponsored jointly by the Welding Research Council, and the Navy Department with funds furnished by the following: American Institute of Steel Construction, American Iron and Steel Institute, Column Research Council (Advisory), Institute of Research, Lehigh University, Office of Naval Research (Contract No. 39303), Bureau of Ships and the Bureau of Yards and Docks.

* See footnote, this page.

† Recently a report on current research projects of the Column Research Council was prepared by Bruce G. Johnston.[3] This report, in addition to giving details of the organization of the Column Research Council, gives a short summary of the objectives of each of the Council-sponsored projects, indicating the results of each obtained to date.

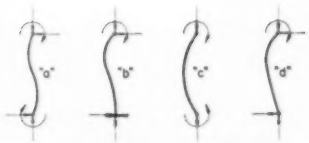


Fig. 1 Conditions of loading studied in the Lehigh Column Investigation

II. THE INTERACTION CURVE CONCEPT

The various loading conditions will be referred to by the use of letter designations to describe the different combinations of axial thrust and end bending moment. These loading conditions are shown in Fig. 1. They are special cases of the general loading shown in Fig. 2.

Figure 2

The strength of a column is a function of many variables and this is true to a greater extent than for common flexural members. One means of expressing the strength of a column subjected to a combination of axial load and end bending moment is by the use of a relationship which has been called an interaction curve.

Two-Dimensional Interaction Curve

Consider a member under axial load

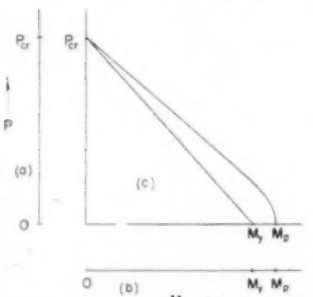


Fig. 3 Initial yield and "simple plastic theory" collapse interaction curves (neglecting the influence of slenderness ratio)

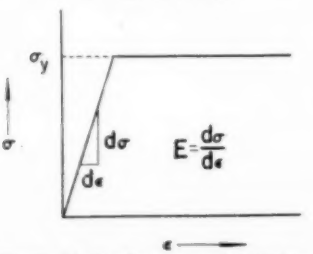


Fig. 4 Idealized stress-strain relation

alone ($M = 0$). If axial load is plotted vertically Fig. 3*a* is obtained. Assuming the idealized stress-strain relation of Fig. 4 collapse occurs at the critical load P_{cr} and, depending on the slenderness ratio of the member, this load is either the yield load P_y (yield-point stress multiplied by the cross-sectional area) or the Euler buckling load, P_e.

Consider next the member under bending moment alone ($P = 0$). If the bending moment, M, is plotted horizontally, Fig. 3(b) is obtained. When $M = M_y = \sigma_y S$, the theoretical initial yield moment of the member will be reached.

The following terminology is used:

M_y = moment at which yield point is reached in flexure.
σ_y = lower yield point stress.
S = section modulus.

According to the simple plastic theory an increase in the applied moment beyond M_y may be sustained until $M = M_p = \sigma_y Z$, where

M_p = full plastic moment.
Z = plastic modulus, the static moment of the entire cross section about the neutral axis.

If Figs. 3*a* and 3*b* are combined, specifying axial load as the ordinate and moment as the abscissa, there results a curve (of which Fig. 3*c* is an example) whereby the relative magnitude of bending moment and axial load may be indicated for various conditions: e.g., initial yield and collapse. In this paper these curves will be referred to as interaction curves.

In the next section of this report, equations are presented for determining the initial yield and collapse relationship between the limiting conditions of zero moment and zero axial load as a function of a number of variables and properties of the section under consideration. The curves shown in Fig. 3*c* are typical of the limiting case, $L/r = 0$, for which $P_{cr} = P_y$.

Thus far in the experimental program, columns have been bent about the strong axis. However, the theories discussed in this paper do not take into account lateral buckling in the inelastic range. Thus, for the usual rolled shapes, these theories are applicable to columns bent about the weak axis. Those which are bent above their strong axis tend to buckle in combined bending and twist. This is a serious limitation in the theory. The development of inelastic lateral buckling (combined bending and torsion) curves to predict behavior under these same loading conditions is considered essential. Several Column Research Council groups are working on related problems.

In present-day engineering practice use of the interaction concept is illustrated by the AISC formula

$$\frac{f_a}{F_a} + \frac{f_b}{F_b} \leq 1 \qquad (1)$$

where

f_a = axial unit stress (actual) = axial load divided by area of member.
f_b = bending unit stress (actual) = bending moment divided by section modulus of member.
F_a = axial unit stress that would be permitted by this Specification if axial stress only existed.
F_b = bending unit stress that would be permitted by this Specification if bending stress only existed.

It is considered that bending moment and axial load are caused by independent conditions. Two other column formulas also might be considered as equations for interaction curves (AASHO,[16] eq 2 and AREA[17] eq 3)

$$f_a = \frac{f_y/\eta}{1 + \left(0.25 + \frac{ec}{r^2}\right) B \operatorname{cosec} \phi_A} \qquad (2)$$

where

f_y = yield point stress in tension.
η = factor of safety based on yield point.
c = distance from neutral axis to the extreme fiber in compression.
r = radius of gyration in the plane of bending.
$\phi_A = \frac{L}{r}\sqrt{\frac{nf_y}{E}}$ radians.
L = effective length of the member.
E = modulus of elasticity of steel.
$B = \sqrt{\alpha^2 - 2\alpha \cos \phi_A + 1}$
$\alpha = \frac{e_s}{e_g}$. When e_g and e_s lie on the same side of the column axis, α is positive; when on opposite sides, α is negative; i.e., when $+e_s = +e_{g1}$, $\alpha = +1$. $e_s = 0$, $\alpha = 0$. $+e_s = -e_{g1}$, $\alpha = -1$.
e_g = eccentricity of applied load at the end of column having the greater computed moment, in.
e_s = eccentricity at opposite end.

$$p = \frac{f_y/f}{1 + \left(\frac{e_1 c_1}{r_1^2} + 0.25\right) \sec \frac{0.875L}{2r_1} \times \sqrt{\frac{fp}{E}} + \frac{e_2 c_2}{r_2^2} \sec \frac{0.875L}{2r_2} \sqrt{\frac{fp}{E}}} \qquad (3)$$

where

p = allowable average compressive unit stress.
r_1 = least radius of gyration of the member.
r_2 = radius of gyration of the member in a direction at right angles with that of r_1.
e_1 = known eccentricity of the applied load in the direction of r_1, in.
e_2 = known eccentricity of the applied load in the direction of r_2, in.
c_1 = distance from neutral axis to extreme fiber in the direction of r_1, in.
c_2 = distance from neutral axis to extreme fiber in the direction of r_2, in.
L = length of the member, in.
E = modulus of elasticity.
f_y = yield point in tension.
f = factor of safety based on yield point.

These equations will be discussed later in further detail.*

* Design formulas have been discussed in a paper by one of the authors[11] with particular emphasis on eccentrically loaded columns and the factor of safety.

Nondimensional interaction curves are of value since they are independent of particular magnitudes of stress, load or moment. Such curves and their development have been discussed by Shanley[15] and others. One such type of curve, that in which the coordinate axes have been chosen as P/P_y and M/M_y, respectively, will be used later in this report in the presentation of test results.

Three-Dimensional Interaction Curve

The interaction curve previously discussed may be expanded to include another important variable, that of length. See Fig. 5. If a z-coordinate axis is added upon which are plotted functions of length (L/r is the abscissa in the length-axial load plane and Ld/bt is the abscissa in the length-moment plane), then for any one rolled shape under a particular loading condition, the surface in space describes the carrying capacity of the column. Since the Ld/bt formula of the AISC is for pure bending, neglecting the effect of flange bending and based on an idealized H-section that neglects the web, it is not completely justifiable to make the comparison indicated. However, it serves to illustrate the concept.

The space surface intersects the three coordinate planes in the curves shown in Fig. 5. The P vs. L/r curve is the familiar column curve. The M vs. Ld/bt curve is the relationship for lateral buckling of beams. Finally the P-M curve is the two-dimensional relationship that has been discussed above. Corresponding curves have been plotted for the AISC formula.

Other Influencing Factors

In addition to lateral buckling, some of the other factors that will influence the results but which have not been considered in this paper are:

(*a*) *Stress-Strain Properties and Residual Stress.* The idealized stress-strain curve of Fig. 4 has been assumed. Even though small coupon test results approximate this assumed condition, the effective stress-strain relationship of the section as a whole may be changed considerably due to the residual stresses present in the rolled section. This will be a function of relative magnitude of axial load and moment. In the experimental program it has been found that the initial yield values predicted by small coupon tests has not been reached when the axial stress was large in relation to the yield point.*

(*b*) *Cross-Sectional Shape.* In solving column problems using the stability approach a knowledge of the influence of cross-sectional shape is required when stresses exceed the elastic limit. In this connection Bleich[9] discusses and presents tentative "shape factors" which could be further developed for various commercially available rolled sections.

(*c*) *Sidesway.* Thus far the investigation has not considered sidesway in the column. However, with a few changes the experimental setup can be made to simulate this type of loading.

(*d*) *Sequence of Loading.* The sequence in which the axial load and end moment are applied may be of possible influence on test results. It is planned to test several columns reversing the sequence of loading to determine the effect of this variable.

(*e*) *Initial Curvature.* For some loading conditions (most important for condition "a") initial curvature would have a pronounced effect on the strength of the member. Thus far in the experimental program, this variable has been of negligible magnitude.

* Recognizing the importance of residual stress in determining the carrying capacity of columns, the Column Research Council through its Research Committee A (Materials) has undertaken the task of establishing and coordinating investigations on the influence of residual stress.

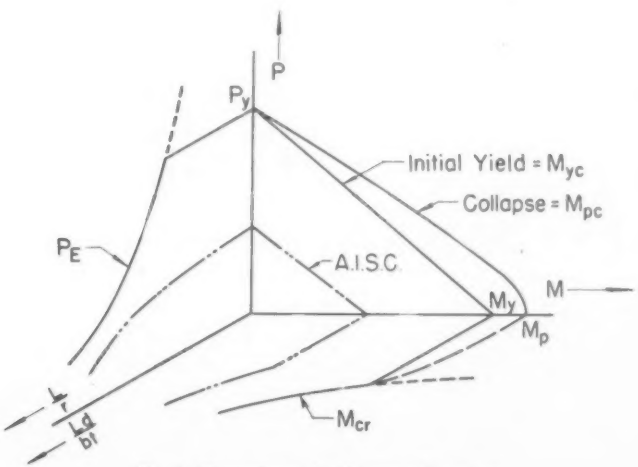


Fig. 5 Three-dimensional interaction curve

III. DERIVATION OF INTERACTION CURVE EQUATIONS

In this section equation are presented for (*a*) initial yield interaction curves, (*b*) a collapse solution based on the simple plastic theory and (*c*) an application of the Jezek approximate solution for the stability problem.

Initial Yield Interaction Curves

Equations for predicting the initial yield load for each of the loading conditions shown in Fig. 1 have been presented in detailed form in an unpublished paper.[4] Except for condition "b" (column fixed at the base) solutions for the conditions may also be determined from a Cornell University Bulletin,[20] making use of graphs, substituting the results in a formula. In Progress Report I.[4] a single formula is derived for each load condition which enables one to compute the coordinates for the interaction curve. Based on the work of D. H. Young, Timoshenko (Reference 19) has presented curves for predicting the yield strength of eccentrically loaded columns. By converting the eccentricity ratio into terms of P, M and the core radius, s, these curves are also useful for load conditions "a," "c," and "d" of Fig. 1.

This section will present the line of reasoning in the development of these equations with a table summarizing the important ones for each loading condition. However, the derivation of equations for initial yielding under loading condition "b" will be shown here. It is assumed that $M_A > M_B$.

The moment applied at the end of the member has been chosen as the interaction curve abscissa. The interaction curve for coupled values of P and M_0 at initial yield will not always be linear since the maximum moment will not necessarily be at the end of the member.

As a starting point, consider a very short member for which L/r approaches zero. Yielding will occur when

$$\sigma_y = \frac{P}{A} + \frac{M_{max}}{S} \qquad (4)$$

Since the length, L, is assumed to be very small, the moment at the end, M_0, will equal M_{max}. Therefore,

$$\sigma_y = \frac{P}{A} + \frac{M_0}{S} \qquad (5)$$

A linear relationship exists between P and M_0 since all other quantities in the above expression are constant for any one particular cross section. The inter-

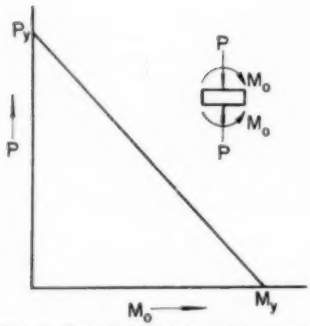


Fig. 6 Initial yield interaction curve neglecting the influence of slenderness ratio

action curve for this case is plotted in Fig. 6.

Next consider the case of a column of length L subjected to the loading shown in Fig. 7*a*. The moment along the column will be composed of two parts—that due to the imposed end moments, M_A and M_B (Fig. 7*b*), and that due to the axial load, P, multiplied by the deflection, y (Fig. 7*c*). The distribution of total moment is shown in Fig. 7*d*. Referring to Fig. 8 the bending moment is a maximum at one end of the column when axial thrust is absent. If the end moments are held constant as the axial load is increased, a load is reached above which the bending moment is no longer a maximum at the column end but at a point between the two ends. Further increase in axial load increases the distance from end A to the point of maximum moment.

The value of axial load, for which the slope of the moment curve at the end of the column is zero (P_2 in Fig. 8*b*) is termed P_L. This case is shown in Fig. 8*b* and is the limiting load for maximum moment at end A. The interaction curve for the range $P < P_L$ will be a straight line since the maximum moment will occur at the end of the column. Two factors which influence the magnitude of P_L are slenderness ratio and loading condition. A more slender column deflects more under bending moment and the moment due to axial load ($P \cdot y$) is correspondingly larger. Thus P_L is lowered. As concerns loading condition, the most severe is load condition "c" for which P_L is always zero.

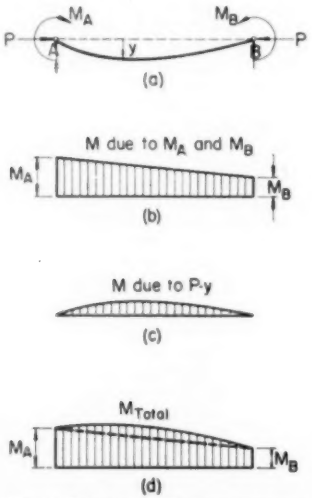


Fig. 7 Column loading and moment diagrams

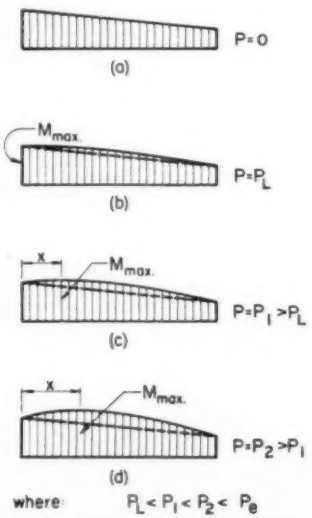


Fig. 8 Effect of axial load on distribution of moment

Loading Condition "b"

The distinguishing feature of this type of loading is that one end is maintained in a "fixed" position; that is, it is not allowed to rotate while moment and axial load are applied. Consider the column with axial load and end moments as shown in Fig. 9. The general equation of the deflection curve (Reference 19, eq 23) for such loading is:

$$y = -\frac{M_B}{P}\left[\frac{\sin kx}{\sin kL} - \frac{x}{L}\right] + \frac{M_A}{P}\left[\frac{\sin k(L-x)}{\sin kL} - \frac{(L-x)}{L}\right] \quad (6)$$

where $k = \sqrt{P/EI}$.

If this equation is differentiated with respect to x an equation is obtained for the slope at any point along the deflected beam. At end B ($x = L$) the slope is zero. Using this boundary condition, an expression for M_B in terms of M_A may be found. The slope equation is given by

Fig. 9 Loading condition "b"

$$\theta_B = y'_{x=L} = -\frac{M_B L\psi}{3EI} + \frac{M_A L}{6EI}\phi \quad (7)$$

where

$$\psi = \frac{3}{u}\left[\frac{1}{\sin 2u} - \frac{1}{2u}\right]$$

$$\phi = \frac{3}{2u}\left[\frac{1}{2u} - \frac{1}{\tan 2u}\right]$$

$$u = \frac{kL}{2}$$

Since $\theta_B = 0$,

$$\frac{M_B L}{3EI}\psi = \frac{M_A L}{6EI}\phi$$

or

$$M_B = \frac{\phi}{2\psi} M_A$$

Substituting this value in the general expression for deflection (eq 6) an equation for this particular loading condition is obtained.

$$y = \frac{M_A}{P}\left[\frac{\sin k(L-x)}{\sin kL} - \frac{(L-x)}{L} - \frac{\phi}{2\psi}\left(\frac{\sin kx}{\sin kL} - \frac{x}{L}\right)\right]$$

Differentiating this deflection expression twice with respect to x results in the equation for curvature along the member

$$y'' = \frac{M_A}{P} \cdot \frac{k^2}{\sin kL}\left[-\sin k(L-x) + \frac{\phi}{2\psi}\sin kx\right]$$

Since

$$y'' = \frac{d^2y}{dx^2} = -\frac{M}{EI}$$

then

$$M = \frac{M_A}{\sin kL}\left[\sin k(L-x) - \frac{\phi}{2\psi}\sin kx\right] \quad (8)$$

By differentiating eq 8, with respect to x and equating to zero, it is possible to solve for the distance to the point of maximum moment in terms of k, ϕ and ψ. Yielding should occur at this point. Then,

$$\frac{dM}{dx} = -\frac{M_A k}{\sin kL}\left[\cos k(L-x) + \frac{\phi}{2\psi}\cos kx\right] = 0$$

Therefore,

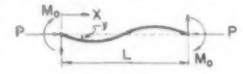
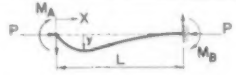
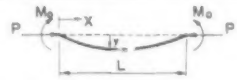
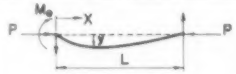
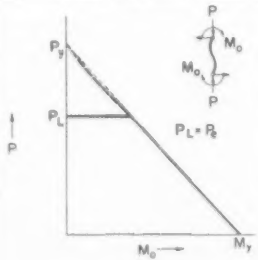
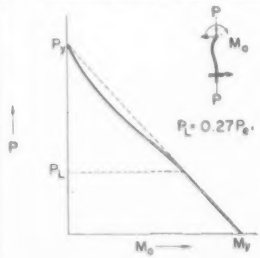
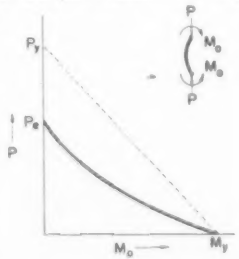
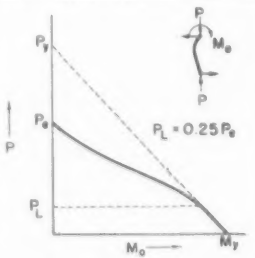
$$\cos k(L-x) = -\frac{\phi}{2\psi}\cos kx$$

or

$$x = \frac{\tan^{-1}\left[\dfrac{-\phi/2\psi}{\sin kL} - \cos kL\right]}{k} \quad (9)$$

If eq. 9 is multiplied by $1/L$, all quantities in the right-hand side of the expression are functions of $2u$ where $2u = kL$. The resulting plot of this equation of $2u$ versus x/L is shown in Fig. 10.

Table 1

FUNCTION	LOADING CONDITIONS "a"	"b"	"c"	"d"
Deflection Curve				
Elastic Buckling Load	$P_e = \frac{\pi^2 E I}{L^2}$	$P_e = \frac{\pi^2 E I}{(0.7L)^2}$	$P_e = \frac{\pi^2 E I}{L^2}$	$P_e = \frac{\pi^2 E I}{L^2}$
P_L	P_e	$0.27 P_e$	0	$\frac{1}{4} P_e$
X_1 = Distance to Maximum Moment ($P > P_L$)	$\frac{L}{2} - \frac{L}{2}\sqrt{\frac{P_e}{P}}$	$\frac{\mathrm{Tan}^{-1}\left[\frac{\phi}{2\psi} - \frac{\mathrm{Cos}\, kL}{\mathrm{Sin}\, kL}\right]}{k}$ *	$\frac{L}{2}$	$L - \frac{L}{2}\sqrt{\frac{P_e}{P}}$
M = Moment at any Point along Column	$\frac{M_o}{\mathrm{Sin}\, kL}\left[-\mathrm{Sin}\, kx + \mathrm{Sin}\, k(L-x)\right]$	$\frac{M_o}{\mathrm{Sin}\, kL}\left[\mathrm{Sin}\, k(L-x) - \frac{\phi}{2\psi}\mathrm{Sin}\, kx\right]$	$\frac{k^2 M_o E I}{P\, \mathrm{Sin}\, kL}\left[\mathrm{Sin}\, kx + \mathrm{Sin}\, k(L-x)\right]$	$\frac{M_o}{\mathrm{Sin}\, kL}\left[\mathrm{Sin}\, k(L-x)\right]$
Maximum Moment	$\frac{M_o}{\mathrm{Sin}\, \frac{kL}{2}}$	Solve for x_1 first, then solve for M_{max}	$M_o\, \mathrm{Sec}\, \frac{kL}{2}$	$\frac{M_o}{\mathrm{Sin}\, kL}$
Interaction Curve Equation	$M_o = S\left[\sigma_y - \frac{P}{A}\right]\mathrm{Sin}\, \frac{kL}{2}$, $P < P_E$ $P = P_E$, $0 < M < M_y\left(1 - \frac{P_E}{P_y}\right)$	Solve for each value of x_1 using $M_o = \left[\sigma_y - \frac{P}{A}\right]\frac{S\, \mathrm{Sin}\, kL}{\mathrm{Sin}\, k(L-x_1) - \frac{\phi}{2\psi}\mathrm{Sin}\, kx_1}$	$M_o = S\left[\sigma_y - \frac{P}{A}\right]\mathrm{Cos}\, \frac{kL}{2}$	$M_o = S\left[\sigma_y - \frac{P}{A}\right]\mathrm{Sin}\, kL$
Typical Interaction Curve ($\frac{L}{r} = 112$)				

* ψ and ϕ values are tabulated in Appendix I "Theory of Elastic Stability" by S. Timoshenko, Mc Graw Hill Co.

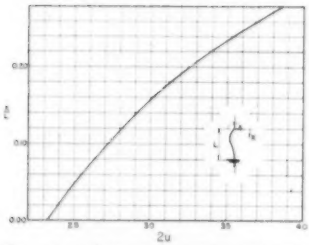


Fig. 10 Solution of eq 9 locating the position of maximum moment as a function of axial load

To find the axial load, P_L, for which the moment gradient is zero at the end of the column, set $x = 0$ in eq. 9. Then,

$$\frac{\phi}{2\psi} = -\cos kL$$

This equation will have a solution (determined graphically) when $2u = 2.33$, or $2u = \pi$. Since the lowest value of P is of interest,

$$2u = 2.33 = kL = L\sqrt{\frac{P}{EI}}$$

from which

$$P_L = \frac{(2.33)^2 EI}{L^2}$$

The buckling load for one end fixed (K^* = 0.7) is given by the equation

$$P_e' \cong \frac{\pi^2 EI}{(KL)^2} = \frac{\pi^2 EI}{(0.7L)^2}$$

Then

$$P_L = 0.27 P_e' \qquad (10)$$

Therefore, as long as P is less than or equal to 0.27 P_e', theoretically the maximum moment will occur at end A of the column.

A typical interaction curve for this condition of loading is shown in Fig. 11. (L/r = 112, has been chosen for illustration.)

* K is the factor which indicates the effective length of the column.

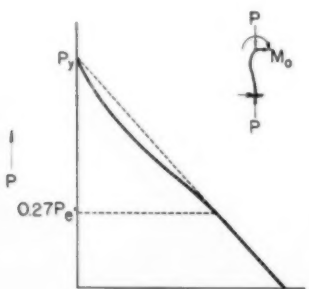


Fig. 11 Initial yield interaction curve for loading condition "b"

Loading Conditions "a," "d" and "c"

Using an approach similar to that for load condition "b" equations may be determined for the three remaining load conditions ("a," "d," "c"). These are summarized in Table 1. Derivations which have been omitted from this paper may be found in Reference 4 which also includes sample calculations.

It is observed that the interaction curve equation for load condition "a" is discontinuous at a load equal to the Euler load for a pin-ended column. In spite of the fact that the column is bent in double curvature with end moments, the column is basically pin-ended since there is no end restraint; end moments are applied but there is no resistance at the ends to rotation under constant moment at the Euler load. In the ideal case under consideration, the deflected configuration is that of double curvature so long as the axial load is less than the Euler load and this has been shown in Table 1. However if a very slight (infinitesimal) initial curvature is assumed, then it will be found that the deflections tend to become infinite at the Euler load for a full length column.* In other words, the curvature degenerates from double curvature to single curvature at this load.

In building frames with continuous connections the columns are actually restrained. So long as yielding does not occur they will carry load above the Euler load until the buckling load of the restrained column is reached. This latter condition is realized when the total stiffness at the joint reduces to zero, and procedures are available for solving this problem. Columns thus restrained may yield at higher loads than predicted neglecting end restraint since the restraint tends to decrease column deflections.

Tests of columns under load condition "a" have been restricted to a range of axial load in which yielding precedes failure due to elastic buckling. As has been mentioned[2] the test apparatus may be used to simulate a range of end restraints. A research program on the behavior of elastically restrained centrally and eccentrically loaded columns is underway at Cornell University.

"Simple Plastic Theory" Collapse Solution for $L/r = 0$†

The length of the specimen under consideration is disregarded (L/r approaches zero). Thus the loading conditions are those of Fig. 12 and the interaction curves developed apply to a particular cross section. So long as the bending moment is a maximum at the end of a column the equations also apply to the column as a whole. Consequently, the following assumptions are made: (*a*) Lateral and local buckling are not considered; (*b*) there is no increase in deflection due to axial load.

* For example, an initial curvature resulting in the equation, $y_0 = \delta \sin \pi x/L$ could be assumed. When axial load is applied, the resulting increment of deflection due to initial curvature is

$$y = \frac{\delta}{1-(P/P_e)} \sin \frac{\pi x}{L}$$

when P approaches P_e this deflection tends to become infinite—a condition of instability.

† The following development is based on hypotheses presented by Baker[2] and Roderick.[14]

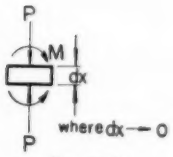


Figure 12

Consider one limit of the problem, in which the axial load, P, is equal to zero. The ultimate moment that could be applied to the "column" according to the simple plastic theory is[1]

$$M_p = \sigma_y Z$$

where Z is the plastic modulus which is the static moment of the section, σ_y is the lower yield point stress and M_p is the plastic hinge moment. The stress distribution implied by this formula is shown in Fig. 13.

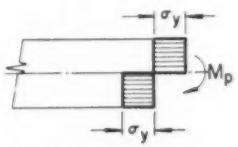


Fig. 13 Assumed stress distribution according to simple plastic theory: Plastic hinge moment

If the moment applied to the section were less than M_p but greater than the initial yield moment, M_y, the typical stress distribution would be as shown in Fig. 14. If

Z_e = static moment of part of section remaining elastic
S_e = section modulus of part of section remaining elastic

then the moment corresponding to the stress distribution of Fig. 14 is

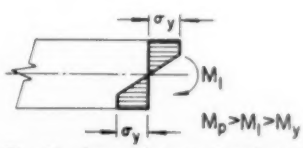


Fig. 14 Assumed stress distribution according to simple plastic theory: Moment less than M_p

$$M_1 = \sigma_y[S_x + Z - Z_x]$$

This equation is derived from beam theory since there is no axial load on the section. In Progress Report No. 1[1] the above expression has been compared with the results of simply supported beam tests.

Consider now the effect on the moment-carrying capacity resulting from the addition of axial load. (NOTE: Only the case of complete plasticity is investigated.)

(a) Neutral Axis Assumed to Be in the Web

The following nomenclature is adopted: (See Fig. 15)

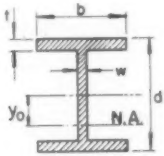


Figure 15

P = applied axial load.
y_0 = distance from center line to neutral axis.
w = web thickness.
Z_P = static moment of portion yielded by axial load P.
t = thickness of flanges

From the known physical dimensions of the section it is possible to determine equations for P and M_s.

The stress distribution at the "hinge" condition that would be caused by a loading condition of end moment plus axial load is shown in Fig. 16. It is assumed that the axial load is such that the neutral axis is in the web and, as shown in Fig. 16 (*b* and *c*), that the total stress distribution is made up of two parts: a stress due to axial load and a stress due to bending moment.

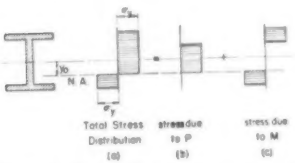


Fig. 16 Stress distribution assumed by simple plastic theory including the influence of axial compression

From Fig. 16*b*, the axial force is given by the expression

$$P = 2\sigma_y y_0 w \qquad (11)$$

The bending moment corresponding to the stress distribution of Fig. 16*c* is

$$M_{pc} = \sigma_y(Z - Z_p) \qquad (12)$$

where $Z_p = wy_0^2$. M_{pc} is the plastic hinge moment modified to include the effect of axial compression.

(b) Neutral Axis Assumed to Be in the Flange

Using the same reasoning as above,

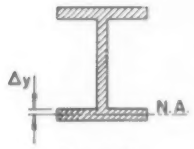


Figure 17

the axial force is given by

$$P = \sigma_y[w(d - 2t) + 2\Delta_y b] \qquad (13)$$

where Δ_y = distance from the inside of the flange to the neutral axis which has been assumed to be in the flange (Fig. 17). The modified plastic hinge value is given by the expression

$$M_{pc} = b\sigma_y(t - \Delta_y)(d - t + \Delta_y) \qquad (14)$$

A typical collapse interaction curve making use of the simple plastic theory for $L/r = 0$ is shown in Fig. 18. For an assumed value of y_0 the coordinates P and M_{pc} may be determined from eqs 11 and 12. When the neutral axis is in the flange, eqs 13 and 14 give the coordinates of the interaction curve.

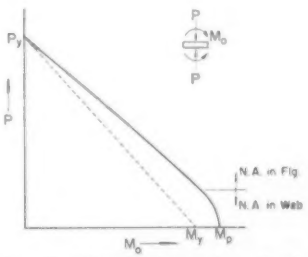


Fig. 18 "Simple plastic theory" collapse interaction curve (neglecting the influence of slenderness ratio)

It is again emphasized that *this solution is only applicable to columns when the moment is maximum at the end of the member and for the range of L/r in which elastic lateral buckling does not occur.* For the various loading conditions Table 1 summarizes values of P_L, the limiting axial load for maximum moment at the end. Certainly above this value, and possibly slightly below it, these equations do not apply for real columns.

Tests of columns bent about the strong $(x-x)$ axis show that a "lateral buckling" type of failure takes place when the axial load is of such magnitude that the moment is not a maximum at the column end* ($P \geqq P_L$). Thus, the value of extending the analysis to include such a case appears academic unless the column is bent about its "weak" axis.

Jezek Buckling Solution for Condition "c"

Loading condition "c" is identical to the eccentrically loaded column with equal eccentricities at each end. F. Bleich,[3] has described procedures of analysis for this condition based on the work of Jezek and Chwalla. Bleich's notation will be used as follows:

σ_{co} = the critical average stress of the centrally loaded column derived from an actual column curve.
σ_c = the critical average stress of the eccentrically loaded column.
$\beta = \dfrac{\sigma_{co}}{\sigma_c}$.

Multiplying the right-hand side of the above equation by A/A.

$$\beta = \frac{A\sigma_{co}}{A\sigma_c} = \frac{P_{cr}}{P}$$

The following approximate equations have been recommended for predicting the buckling load of structural steel columns with a lower yield point of 40 ksi:

for $L/r = 20$ to 75,

$$\beta = \left(1 + \frac{\kappa}{2}\right) + \frac{\sqrt{\kappa}}{9000}\left(\frac{L}{r}\right)^2 \qquad (15)$$

for $L/r = 95$ to 200,

$$\beta = 1 + \frac{10{,}600}{(L/r)^2}\sqrt{\kappa^2} \qquad (16)$$

where

κ = eccentricity ratio.

$$\kappa = \frac{e}{\delta} = \frac{ec}{r^2} = \frac{M_0 c}{Pr^2}. \qquad (17)$$

Consider the range of L/r from 20 to 75. Introducing a shape factor of 1.3, as tentatively recommended by Bleich[3],* from eq 15:

$$\beta = \left(1 + \frac{1.3\kappa}{2}\right) + \frac{\sqrt{1.3\kappa}}{9000}\left(\frac{L}{r}\right)^2$$

Solving this equation for κ and making use of relation 17 the following equation relating M and P is obtained:

$$M_0 = \frac{Pr^2}{1.3c}\left[\frac{-(L/r)^2}{9000} \pm \sqrt{\frac{(L/r)^4}{(9000)^2} - 2\left(1 - \frac{P_{cr}}{P}\right)}\right]^2 \qquad (18)$$

* This "shape factor" is not the value of $f = M_p/M_y$ as used in simple plastic theory, but is a factor to be applied to the term κ dependent on the cross-sectional form and flexure axis.

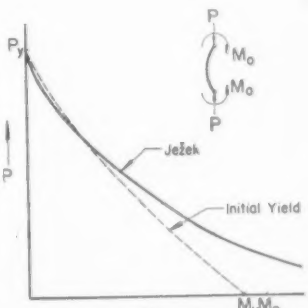


Fig. 19 Jezek buckling interaction curve

* Of course, lateral buckling may occur when P is less than P_L.

The deviation of eq 18 from an exact solution becomes appreciable for small values of κ, but decreases rapidly as M increases.

A plot of eq 18 for a particular column (8WF31 shape $L/r = 55$) is shown in Fig. 19 by a solid line.

Discussion of Interaction Curves

Equations for computing interaction curves according to the "initial yield" criterion were presented above for the four standard loading conditions and are summarized in Table 1.

The influence of length on each load condition is shown in Fig. 20. For a certain moment the axial load at which yielding will occur decreases as the slenderness ratio increases. However, for each loading condition except "c" (single curvature) there is a range of axial load which is theoretically unaffected by slenderness ratio. The three-dimensional interaction curve of Fig. 21 illustrates this range for one particular loading condition. It is that region defined by the plane *efghij*. In this figure end bending moment versus axial load versus length of column has been plotted for a 4WF13 shape, the member being loaded under condition "d." This surface defined by the straight lines *ef*, *gh* and *hi* and the curve *ije* is a plane (assuming the idealized stress-strain diagram shown in Fig. 4). Then for any point on this surface, the load-carrying capacity as determined by initial yielding is not a function of length of the column. The problem lies in the fact that the real column curve, for as-delivered members, does not have a straight horizontal portion at the load P_y. The same is true of the lateral buckling curve, M_{cr} versus Ld/bt.

Figure 20 (loading condition "d") amounts to cross curves of Fig. 21 cut by a plane parallel to the P-M plane and at various slenderness ratios. Timoshenko (Reference 18, pp. 40–43) has used cross curves parallel to the P-L/r plane and at various moment-values to show the influence of end eccentricity of load upon the initial yield strength of columns. Such curves have been used by the AASHO in their column design specifications. In preference to this method, interaction curves such as those shown in Fig. 18 have been used for presentation of experimental and theoretical results in this current series of tests since two criteria of failure (yielding and collapse) may be compared and the method of loading may be clearly indicated in such figures.

In comparing the initial yield strengths for the various loading conditions (see Fig. 22), it is evident that below the Euler buckling load, the severity of loading condition increases in the order "a," "b" "d" and "c."

The various P_L-values (the thrust below which the bending moment is a maximum at the column end) have been indicated in

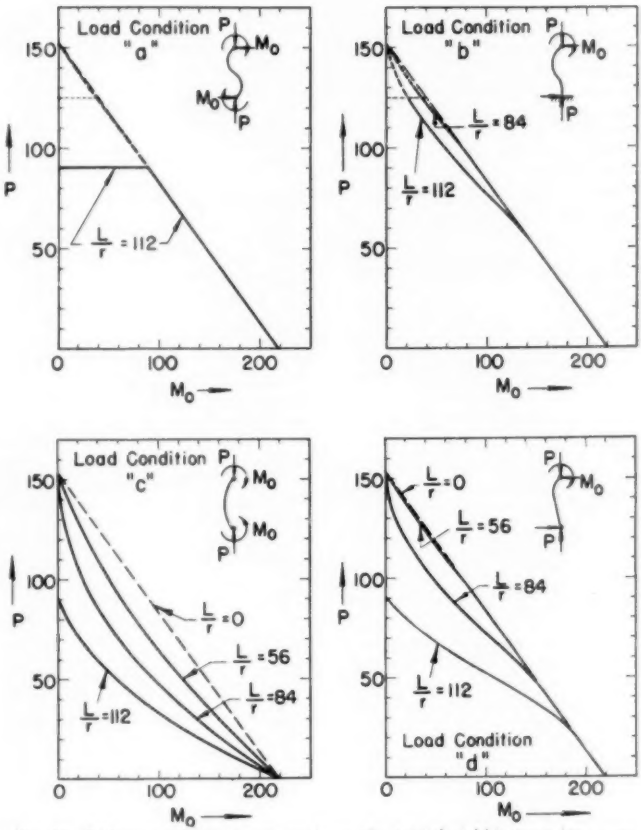


Fig. 20 Influence of slenderness ratio on the initial yield interaction curve

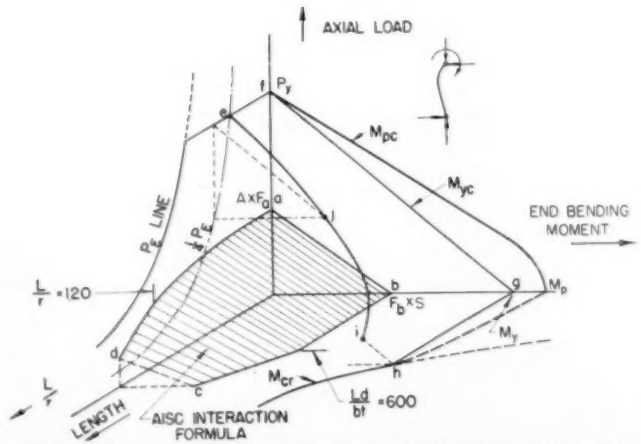


Fig. 21 Three-dimensional interaction curve for loading condition "d"

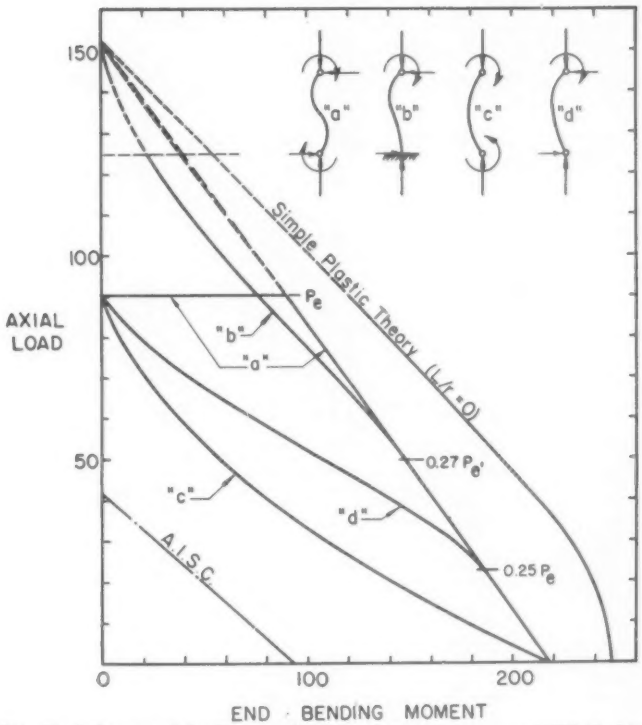


Fig. 22 Influence of loading condition on interaction curves (4WF 13, L/r = 111)

Fig. 22 and these have also been tabulated in Table 1. Since the modified Euler load for flexure about the y-y axis (125 kips) is less than the yield point load (150 kips), the maximum load which 4WF13 columns may sustain under loading condition "b" is 125 kips. Thus the curve has been dotted above this load. The effect of restraining moment at one end is quite pronounced as may be seen by comparing "b" with "a," "d" and "c." The dotted portion of the load condition "a" interaction curve (extended) shown in Figs. 20*a* and 22 would be realistic only if the column were assumed laterally supported at the mid-height.

Collapse solutions for eccentrically loaded columns based on a more rigorous plastic theory, such as along the lines, advocated by Jezek and Chwalla,[9] have not been included in this report. This type of procedure has been followed by P. P. Bijlaard[8] in the current investigation of Rigid Joint Structures at Cornell University. Their results are applicable to members bent about the weak axis and restrained about the same axis. Later tests at Lehigh in which the effect of flexure about the weak axis is studied will be compared with the theoretical and experimental results of the Cornell work.

IV. COMPARISON OF INITIAL YIELD INTERACTION CURVES WITH DESIGN SPECIFICATIONS

Present design specifications for the type of steel structures encountered in civil engineering practice are in general based on initial yield as the criterion of failure. Therefore, a comparison will be made with the design formulas mentioned earlier in this paper and the initial yield formulas. If the end bending moments are considered as moment due to end eccentricity of load and/or initial curvature, certain of the design formulas should be in agreement with some of the equations summarized in Section III of this paper (load conditions "a," "c" and "d") since they are direct applications of the initial yield criterion based on the work of Timoshenko and Young.

AASHO Column Specifications[15]

$$\frac{P}{A} = \frac{f_y/\eta}{1 + [0.25 + (e_y c/r^2)]\, B \operatorname{cosec} \phi_A}$$

where

η = factor of safety.

$B = \sqrt{\alpha^2 - 2\alpha \cos \phi_A + 1}$.

α = ratio of the applied end moments ($\alpha_a = -1$, $\alpha_c = +1$, $\alpha_d = 0$, the subscripts referring to the load condition shown in Fig. 1).

$$\phi_A = \frac{L}{r}\sqrt{\frac{P\eta}{AE}}$$

Assuming that the initial end moment, M_0, for the column under investigation is caused by a known eccentricity, e_y, and an assumed additional eccentricity ratio in the plane of the known eccentricity, $e/s = 0.25$, to compensate for end load eccentricity, secondary moments and accidental crookedness. Then,

$$M_0 = P\left(\frac{0.25r^2}{c} + e_y\right)$$

Substituting this in the AASHO formula above and noting that $\phi_A = kL\sqrt{\eta}$, the following equation is obtained:

$$\frac{f_y}{\eta} = \frac{P}{A} + \frac{M_0 B}{S} \operatorname{cosec} kL\sqrt{\eta}$$

Since a correlation with the initial yield condition is sought then $\eta = 1$, and the above equation reduces to

$$M_0 = \frac{S}{B} \sin kL \left[\sigma_y - \frac{P}{A}\right]$$

in which f_y has been replaced by σ_y. For the various loading conditions defined by B, this equation is identically equal to those previously derived. For example, consider loading condition "c," $\alpha_c = 1$,

$$M_c = \frac{S}{\sqrt{2 - 2\cos kL}} \sin kL \left[\sigma_y - \frac{P}{A}\right]$$

$$= \frac{S \sin kL}{2\sqrt{\frac{1 - \cos kL}{2}}} \left[\sigma_y - \frac{P}{A}\right]$$

$$= \left[\frac{2S \sin kL/2 \cos kL/2}{2 \sin kL/2}\right] \left[\sigma_y - \frac{P}{A}\right]$$

$$M_0 = S \cos \frac{kL}{2} \left(\sigma_y - \frac{P}{A}\right)$$

as given in Table 1.

AREA Specification for Structural Steel Compression Members[17]

$$p = \frac{f_y/f}{1 + \left(\frac{e_1 c_1}{r_1^2} + 0.25\right) \sec \frac{L_c}{2r_1} \times \sqrt{\frac{fP}{AE} + \frac{e_2 c_2}{r_2^2} \sec \frac{L_c}{2r_2}\sqrt{\frac{fP}{AE}}}}$$

where f = factor of safety (corresponding to η in AASHO specifications).

The major differences between the AASHO and the AREA formulas are that in the latter (1) bending is assumed about both axes of the section and (2) only load conditions "c" is implied. To obtain the maximum effect, the member is assumed to have initial curvature in the weak direction.

Making the assumption that bending in the plane perpendicular to the web is caused by a known eccentricity, e_1, and an additional eccentricity ratio $e/s = 0.25$, and that bending in the plane of the web is caused only by a known eccentricity, e_2, the equation for moment in

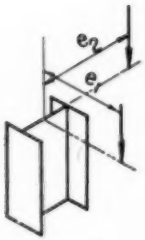


Figure 23

each of these plates is (see Fig. 23):

$$M_{y_1} = P\left(\frac{0.25r_1^2}{e_1} + e_1\right)$$
$$M_{y_2} = P(e_2)$$

Therefore,

$$\frac{f_y}{f} = \frac{P}{A} + \frac{M_{y_1}}{S_1}\sec\frac{kL}{2}\sqrt{f} + \frac{M_{y_2}}{S_2}\sec\frac{kL}{2}\sqrt{f}$$

For the initial yield condition, $f = 1$. Replacing f_y by σ_y, the general equation reduces to

$$\sigma_y = \frac{P}{A} + \frac{M_{y_1}}{S_1}\sec\frac{kL}{2} + \frac{M_{y_2}}{S_2}\sec\frac{kL}{2}$$

which is the same equation as that developed in Section III for load condition "c" with the terms arranged somewhat differently, the equation being extended for the case of bending about both axes.

AISC Interaction Formula[13]

$$\frac{f_a}{F_a} + \frac{f_b}{F_b} \leq 1$$

where

$f_a = P/A$.
$f_b = Mc/I$.
$F_a = 17{,}000 - 0.485(L/r)^2$.
$F_b = \dfrac{12{,}000{,}000}{Ld/bt}$ for $Ld/bt > 600$.
$= 20{,}000$ for $Ld/bt < 600$.

The Ld/bt formula was derived for a uniformly loaded, simply supported beam. Also, the limiting column stress in the AISC Interaction Formula is defined by a parabolic formula which allows inherently for the factor of safety. In Fig. 21 graphical comparison is made for one loading condition and size of member, the surfaces mapped by the AISC formula and the theoretical interaction curve being shown. Throughout the report the AISC interaction formula has been plotted on theoretical and experimental curves using the radius of gyration of the axis about which bending was applied.*

* In order to produce failure with the bending occurring about the stronger axis, knife edge supports were located parallel to this axis for all tests reported herein. Had the effective length of the column, KL, been the same for both radii of gyration, as usually is the case in most engineering structures, the value of F_a would be determined by r_y rather than r_x, thus affording an additional factor of safety.

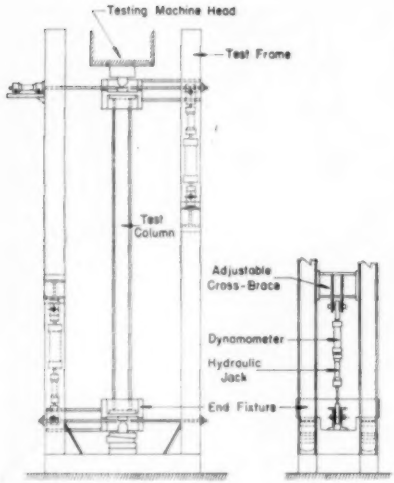


Fig. 24 Test apparatus

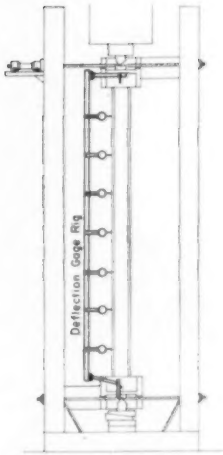


Fig. 25 Deflection gage rig

V. EXPERIMENTAL INVESTIGATION

Description of Test Apparatus and Procedure

Details of the test apparatus and testing procedure are described in Reference 2 which also includes an outline of the original test program. One figure has been reproduced here for illustration of the test method. In order that end bending moment could be applied independently of axial load, the apparatus shown in Fig. 24 has been used in the experimental part of the investigation. A recent change has been the use of dial gages instead of taut-wire and mirror gages for measuring deflections. In these tests, deflection measurements in the strong direction were made by use of a deflection rig (Fig. 25). This rig is suspended on a rod located on the plane of the upper base of the column at its center line. It is allowed to slide on another rod located in the plane of the lower base of the column also at the center line. By using this system all readings are taken with respect to the original axis of the column. A disadvantage of the previously used method was partly its lack of sensitivity and partly the difficulty of reading the gages during the test because of limited working space around the test column.

For measuring deflections in the lateral direction the arrangement shown in Fig. 26 is used, dial gage extenders being clamped to the flanges of the column. This is a technique commonly used by the Cambridge University group. One end of the extender is a piece of spring steel twisted through 90 deg, thereby assuring freedom from restraint in all directions. This spring steel is in turn riveted to an aluminum rod, the other end of which contains a recess to receive the plunger of the dial gage. With this method for measuring lateral deflection, twisting motion may also be followed.

With one exception all tests have been conducted by first applying the axial load to the desired value. This load has then been maintained constant while the bending moment is increased to collapse.

Column Tests Completed

Table 2 presents a summary of the column test results. Two geometrically similar sections were chosen for investigation, 8WF31 and 4WF13. Using these sections with lengths of 8, 12 and 16 ft

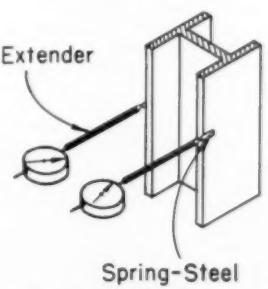


Fig. 26 Lateral deflection gage arrangement

Table 2—Summary of Test Results

Test No.	Section	Length, ft	L/r_z*	Failure loading condition	P/P_u*	M/M_u*	Type of failure	Obsv. yield strength† / Calc. initial yield	Obsv. maximum load† / Calc. collapse load	Figure No.	Obsv. maximum load† / AISC spec.
Pilot	4WF13	16	111.6	"b"	0.26	...	Bending plus torsion	...	0.85	31	...
1	8WF31	6	20.6	"d"	0.13	...	Bending‡	1.09	1.06	28	2.53
2	8WF40	6	20.4	"b"	0.14	...	Bending‡	0.98	1.11	29	2.53
3	8WF31	16	55.4	"b"	0.50	...	Bending, torsion and local buckling	1.00	1.00	30	2.61
4	8WF31	16	55.2	"b"	0.12	...	Bending plus local buckling	1.00	1.00	30	2.46
5	8WF31	16	55.3	"b"	0.80	...	Bending plus torsion	0.77	0.86	30	2.33
6	4WF13	16	111.7	"b"	0.26	...	Yielding prior to testing				
7	4WF13	16	110.9	"b"	0.26	...	Bending plus torsion	0.96	0.85	31	2.41
8	8WF31	16	55.1	"c"	...	0.13	Bending plus torsion	0.74	0.75	32	2.00
9	4WF13	16	111.0	"b"	0.10	...	Bending plus torsion	1.01	0.9	31	2.20
10	4WF13	16	111.0	"b"	0.49	...	Bending plus torsion	0.78	0.69	31	2.26
11	8WF31	16	55.2	"c"	...	0	Bending plus torsion	0.77	0.87	32	2.46
12	8WF31	16	55.2	"c"	0.12	...	Bending plus torsion	0.64	0.83	32	2.05
13	8WF31	16	54.9	"d"	0.12	...	Bending plus local buckling	1.15	1.08	33	2.68
14	8WF31	16	55.2	"a"	0.12	...	Bending plus local buckling	0.97	1.00	34	2.62
15	8WF31	12	41.3	"c"	...	0	Bending, torsion and local buckling	0.69	0.84	35	2.10
16	8WF31	12	41.2	"c"	0.12	...	Bending, torsion and local buckling	0.91	0.82	35	2.00

* These values based on measures cross-section dimension.
† Determined from radial lines through the experimental points.
‡ Test not continued sufficiently to develop inelastic local buckling.

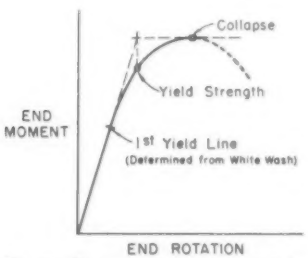


Fig. 27 Various graphical criteria for comparing the strength of columns

has made it possible to test columns in a range of slenderness ratio extending from $L/r = 27$ to 111 for flexure about the strong axis.

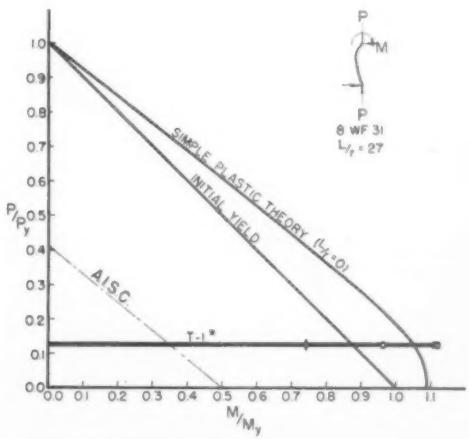
Test Results

The results of the tests listed in Table 2 are presented in Figs. 28 through 35. Here the experimentally determined strength values are compared with interaction curves. Results are presented according to three criteria:

1. First observed yield line (other than local yielding at the ends).
2. Yield strength.
3. Collapse.

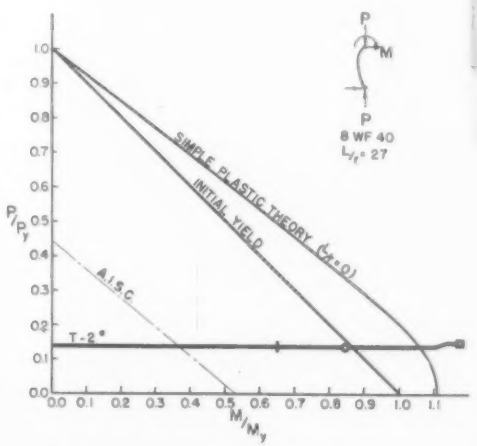
These conditions are defined graphically on the moment-rotation curve of Fig. 27. The method shown for determining yield strength was previously described by one of the authors.[11]

Double weight lines are shown on the interaction curves to indicate the path chosen for attaining a certain position on the interaction curve. At the point at which the first yield line was observed, a dash perpendicular to the direction of testing is shown. At the point corresponding to the yield strength a circle is shown. The collapse load has been indicated by a square.



* Length of Column T-1 was 6'-0"

Fig. 28 Results of T-1 and interaction curves for loading condition "d"



* Length of Column T-2 was 6'-0"

Fig. 29 Results of T-2 and interaction curves for loading condition "d"

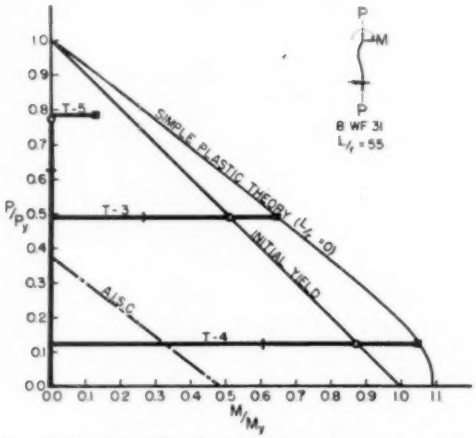
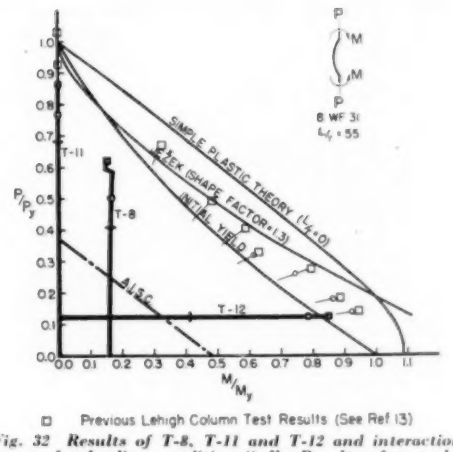


Fig. 30 Results of T-3, T-4 and T-5 and interaction curves for loading condition "b"



□ Previous Lehigh Column Test Results (See Ref 13)

Fig. 32 Results of T-8, T-11 and T-12 and interaction curves for loading condition "c." Results of tests by Johnston and Cheney[13] also shown

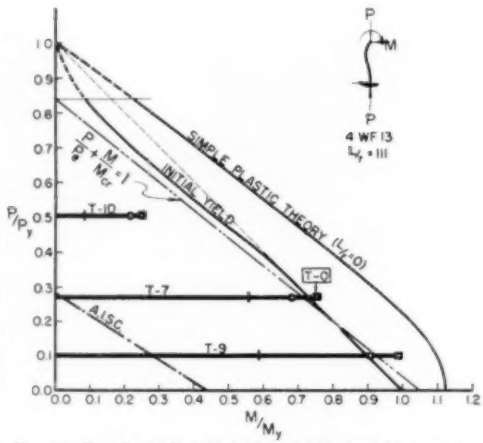


Fig. 31 Results of T-9, T-7, T-9 and T-10 and interaction curves for loading condition "b"

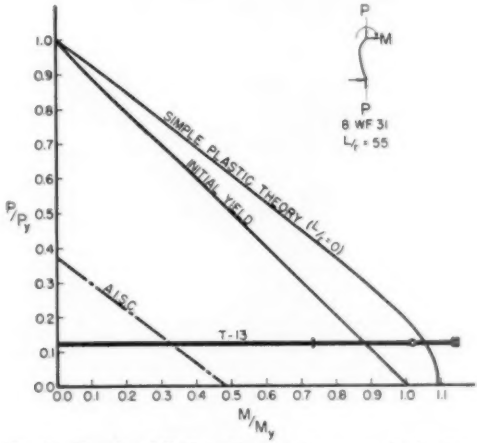


Fig. 33 Results of T-13 and interaction curves for loading condition "d"

Test 6 has been omitted since the column yielded due to flexure in the weak direction during alignment prior to testing, undoubtedly reducing the carrying capacity of the column.

Test 8 (Fig. 32) was the only column tested thus far with the application of bending moment preceding axial thrust. A moment was first applied to a certain value and was maintained constant while axial load was increased to collapse.

Test 14 (Fig. 34) was stopped after having just reached the predicted collapse value because of the difficulty of providing enough lateral force at the ends of the member to counteract the high shear forces developed by load condition "a." After realigning the column by eye, a second test was performed allowing the axial load to increase. This is shown by the dashed line on the same figure.

VI. DISCUSSION OF TEST RESULTS AND SUMMARY

The results will now be discussed in the sequence of the figures. Although there are other objectives for specific tests, the principal variables studied here are the following: (*a*) the influence of distribution of moment on column strength, (*b*) the influence of relative magnitude of axial load on column strength. With a few notable exceptions the latter factor has been deemphasized in the latter tests, the P/P_{cr} ratio being kept low pending further theoretical work.

Test 1 (Fig. 28) (8W31, L/r = 27, Condition "d")

This test simulates a column in a single bay portal frame with pinned bases. The analogy explains the selection of the relatively low ratio of P/P_{cr}. Theoretical interaction curves for this short column are identical in the "a", "b" and "d" loading conditions. This is due to the fact that the maximum moment is always at the end at which the moment is applied.

The moment carried by *T-1* was con-

siderably in excess of that predicted; further moment could have been sustained since the test was stopped while the moment was still increasing. This increase is undoubtedly due to strain hardening, an effect more pronounced in this test than in those which follow, because of the more severe moment gradient than that present in the longer columns. In each test the theoretical length of the plastic zone is given by the expression $L(1\text{-}M_y/M_p)$. Thus for the shorter column the plastic zone is shorter, and the effective strain-hardening tendency is greater.

Test 2 (Fig. 29) (8WF40, L/r = 27, Condition "d")

This test was intended as a preliminary to a portal frame test in which the column bases are pinned. Both Tests 1 and 2 indicate that there is a range in which the ultimate strength in bending is unaffected by axial load.

Tests 3–5 (Fig. 30) (8WF31, L/r = 55, Condition "b")

T-3 and *T-4* developed the predicted collapse values, failing eventually due to local buckling after strain hardening commenced. *T-5* did not develop the full strength, buckling sidewise due to combined bending and twist. Residual stress is a factor in this failure as yielding commenced in *T-5* while axial load was increasing, prior to moment application.

A residual stress level of about 12 ksi was present as determined by a separate series of measurements. On the P/P_y and M/M_y coordinates this would correspond to a ratio of 0.7. The "observed first yield lines" are in as close agreement with the ratio as could be expected.

Pilot Test 0 and Tests 7, 9, 10 (Fig. 31) (4WF13, L/r = 111, Condition "b")

These 16-ft, 4WF13 columns of greater slenderness ratio than those in Fig. 30 failed by lateral buckling, none reaching the "modified plastic hinge" value. Thus the influence of slenderness ratio is quite pronounced over the range L/r = 111 to L/r = 55.

Considering first the theoretical initial yield curve, the departure from a straight-line relationship is a measure of the influence of the additional moment due to deflection of the column.

Above about 125 kips (P/P_y = 0.84) the "initial yield" and "ultimate strength" curves have no real meaning since this is the maximum value the column can carry under axial load, and theoretically, collapse should occur in the "weak" direction. Figure 33 also shows the straight-line lateral buckling relationship discussed in the work of Hill and Clark[18] and is applicable for the case of equal moments at the ends. *T-7*, *T-0* and *T-9* indicate strengths greater than predicted on the straight-line basis, and this is probably due to the fixed-end loading condition. The collapse load for *T-10* is somewhat below that predicted by the straight-line interaction formula for "lateral buckling." The presence of residual stresses is expected to reduce the lateral buckling strength as the axial load is increased.

The agreement between Pilot Test *0* and *T-7*, indicates that there was no influence on ultimate strength of prior plastic deformation. Conditions were identical except that *T-0* had been plastically deformed about the *x-x* axis earlier in the pilot program.

Tests 8, 11, 12 (Fig. 32) (8WF31, L/r = 55, Condition "c")

The straight-line lateral buckling equation would apply directly to this loading condition. However, P_r'/P_y and M_{cr}/M_y are both greater than 1.0; thus the curve is not shown. The Jezek solution described earlier is shown in this figure.

T-8 was conducted as a comparison for a test carried out at Cambridge University, England. It constitutes the only test done thus far in which the end moment was maintained constant while axial load was increased to collapse.

Comparing *T-12* (Fig. 32) with *T-4* (Fig. 30), even at the low ratio of P/P_y the distribution of moment becomes an important factor.

T-11, together with a later axial load tests, reflect the influence of residual stress. The maximum theoretical strength for M = 0 is P_y when locked-up stress are neglected. According to the column curve in Progress Report C,[6] this maximum strength should be reduced about 1% based on measured coupon stress-strain properties. The actual observed reduction is about 15% and it is considered that residual stresses are primarily accountable.*

The first yield lines were observed at relatively lower end moments in the series shown in Fig. 32. One reason is that the maximum moment is at the center of the column; of all the loading conditions, this results in the greatest difference between the magnitude of end

* The influence of residual stress on the behavior of steel columns is also being considered in a separate investigation. The results of *T-11* and *T-15* have also been plotted on a column curve which takes residual stress into account on a theoretical basis; it is presented in Progress Report M.[5]

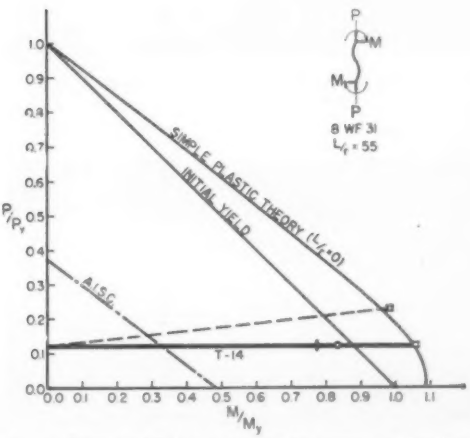


Fig. 34 Results of T-14 and interaction curves for loading condition "a"

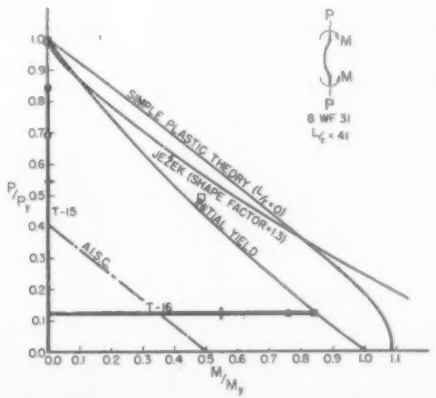


Fig. 35 Results of T-15 and T-16 and interaction curves for loading condition "c"

moment and of maximum moment. Condition "c" is clearly the most critical condition for columns bent about the strong axis.

Test 13 (Fig. 33) (8WF31, L/r = 55, Condition "d") and Test 14 (Fig. 34) (8WF31, L/r = 55, Condition "a")

Together with *T-4* (Fig. 30) and *T-12* (Fig. 32) this completes a series at one P/P_{cr} value on the influence of type of loading on the behavior of 16-ft columns. Test 14 was stopped after the predicted plastic moment value was reached due to the concurrent development of high shear forces which overloaded the testing frame in the case of load condition "a." Only under condition "c" loading did the columns fail to develop the predicted yield strength.

In *T-14* a second loading was carried out as mentioned in the previous section. The prior plastic deformation did not prevent the member from developing its predicted ultimate strength under the new loading conditions.

Tests 15 and 16 (Fig. 35) (8WF31, L/r = 41, Condition "c")

These tests are comparable with the *T-8*, *T-11* and *T-12* series of Fig. 32 except that *L/r* has been decreased from 55 to 41 by use of a 12-ft column. The results are similar to those obtained for the tests of longer members under this loading condition.

Selected Previous Column Tests Results

A large number of eccentrically loaded columns were tested in a previous investigation[12] at Lehigh University. In each test the eccentricities were equal and on the same side of the longitudinal axis (load condition "c"). The results of those tests for which the slenderness ratios were near the values of 55 and 41 are plotted in Figs. 32 and 35, respectively. Only columns bent about the strong axis are included. In each of these tests both yielding and collapse strength are appreciably higher than those observed in the current investigation. This difference may be due to the effect of shape of cross section which also influences the level of residual stress present. A 315.7 shape was used in the earlier program.

General Discussion

Two general modes of failure were observed in the tests: (*a*) Combined bending and torsion (often referred to as "lateral-torsional buckling"), and (*b*) bending plus local buckling. Figures 36 and 37 show columns which have collapsed due to combined bending and torsion which is typical for those columns which were loaded in single curvature (condition "c" loading). The failures shown in Figs. 38 and 39 involve local buckling of the flange elements. They are typical of columns deformed in double curvature and columns in which the bending moment is a maximum at the end of the column. The load-carrying capacity equals or exceeds that predicted by the theory; the local buckling shown prevents the member from carrying additional load.

Fig. 36 Column failure (lateral buckling) under loading condition "c"

Fig. 37 Column failure (lateral buckling) under loading condition "b"

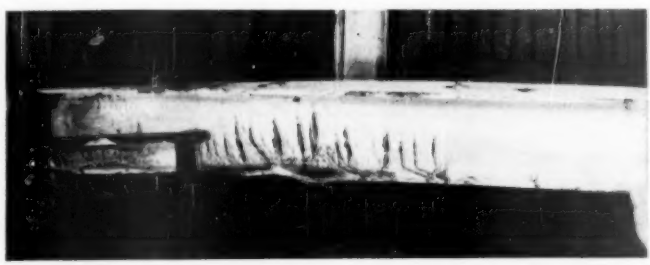


Fig. 38 Column failure (local buckling) under loading condition "b"

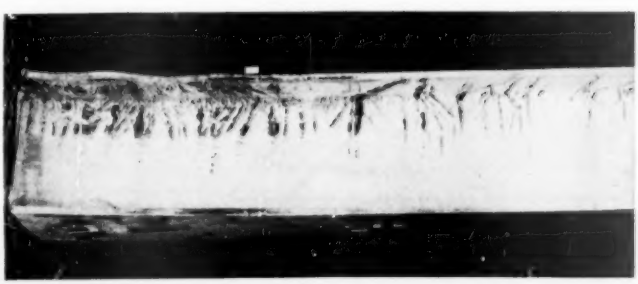


Fig. 39 Column failure (local buckling) under loading condition "d"

As stated previously, a summary of the test results has been shown in Table 2. Here loading condition, P/P_y, L/r and type of failure for each specimen have been listed. Except for short columns with relatively low axial loads tested under conditions "a" "b" and "d," failures have been of the "lateral buckling" type.

Regardless of L/r or P/P_y the single curvature loading condition "c" has in all cases resulted in failure by lateral-torsional buckling. The tendency toward this mode of failure was most pronounced when the value of P/P_y was relatively high. The reduction in failure load is in part due to residual stresses in the member.

In all cases in which the strength predicted by the simple plastic theory was reached, the final failure was due to local buckling.

When the maximum moment occurs at the end of the member, at least the predicted initial yield value has been reached for these tests regardless of the mode of failure (see, for example, *T-7*, *T-9* and *T-10*; Fig. 31). For the condition of single curvature, the column will not develop its yield strength unless axial load is relatively low (see Figs. 32 and 35).

Further Studies

In addition to such variables as influence of load condition and ratio of P/P_{cr} for various slenderness ratios, the following influencing factors are currently receiving attention in the experimental and analytical program:

1. Sequence of loading.
2. Size of cross section.
3. Bending about the minor (y-y) axis.
4. Residual stress.
5. Rotation capacity.

By the last item is meant the ability of a column to sustain additional end rotations after the maximum moment has been reached. The information is needed from both static and dynamic consideration.

SUMMARY

This research program is still under way and consequently the conclusions cannot amount to more than a summary of the trends that have been observed from a limited number of tests.

1. Initial yield interaction curves have been developed and summarized for a number of standard loading conditions.

2. A method for determining the collapse interaction curve, excluding the influence of slenderness ratio, has been reviewed.

3. Derivations included in this report do not take into account lateral-torsional buckling. Further analytical work is needed to include this important mode of failure.

4. Two general types of instability failure have been observed in these tests involving flexure about the strong axis. These are lateral-torsional buckling and local buckling. In those cases in which the strength predicted by the simple plastic theory was reached, the final collapse was a result of buckling of the compression flange elements, providing the test was carried to collapse.

5. Below the Euler buckling load the severity of loading condition increases in the order "a," "b," "d" and "c." Likewise for the same (L/r) the tendency toward a lateral-torsional mode of failure increase in the same sequence. In each of the tests under condition "c" loading, lateral-buckling brought about final collapse.

6. The predicted yield value for columns bent in single curvature (condition "c") was obtained in tests of this investigation when axial load was relatively low. The reduction in strength at larger values of axial load is believed to be due to a number of variables. These are residual stresses, variation of cross-sectional properties and initial curvature of the member. Such variations may or may not be within rolling tolerances, however their contribution to stress reduction will be the greatest for condition "c." In the present investigation the latter two factors have not been of sufficient magnitude to appreciably influence test results.

7. With the exception of one test (*T-5*), when the maximum moment occurred at the end of the column the predicted initial yield strength was realized regardless of the mode of failure.

8. The influence of residual stress is more important than previously realized in reducing the strength of columns below values predicted on the basis of small coupon tests.

9. There is a range of loading conditions in which steel columns will carry more load than predicted by the theories herein discussed and another range in which columns collapse below the predicted yield strength (see Table 2). Since most column formulas are based upon stress as a criterion, this indicates that the formulas may be too safe on the one hand but may not be overconservative for a different range.

VII. ACKNOWLEDGMENTS

The authors wish to acknowledge the contributions made toward the investigation by the work of Chien-Huo Chen, Jan M. Ruzek and Edmound L. Kaminsky. The helpful criticisms of members of the Welding Research Council Lehigh Project Subcommittee (T. R. Higgins, chairman) and, the Column Research Council Research Committee D (N. M. Newmark, chairman) are sincerely appreciated. This program is being carried out in the Fritz Engineering Laboratory of which Prof. Wm. J. Eney is Director.

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IX. NOMENCLATURE

A = gross cross-sectional area.
b = flange width.
c = distance from neutral axis to extreme fiber. (AASHO) Distance from neutral axis to the extreme fiber in compression.
c_1 = (AREA) distance from neutral axis to extreme fiber in the direction of r_1.

c_2 = (AREA) distance from neutral axis to extreme fiber in the direction of r_2.
d = depth of section.
E = Young's modulus of elasticity.
E_t = tangent modulus.
e = eccentricity of loading.
e_1 = (AREA) known eccentricity of the applied load in the direction of the smaller value of r (r_1).
e_2 = (AREA) known eccentricity of the applied load in the direction of the greater value of r (r_2).
e_a = (AASHO) eccentricity of applied load at the end of the column having the greater computed moment, in.
e_b = (AASHO) eccentricity at opposite end.
f = (AREA) factor of safety based on yield point.
f_b = (AASHO) yield point stress in tension.
f_a = (AISC) axial unit stress (actual) = axial load divided by area of member.
f_b = (AISC) bending unit stress (actual) = bending moment divided by section modulus of member.
F_a = (AISC) axial unit stress that would be permitted by this specification if axial stress only existed.
F_b = (AISC) bending unit stress that would be permitted by this specification if bending stress only existed.
I = moment of inertia.
L = total length of a column. (AASHO) effective length of the member.
L/r = slenderness ratio.
Ld/bt = ratio governing allowable compressive stress in flanges as specified by the AISC.
M = moment.
M_{cr} = critical bending moment for the beam under pure bending alone.
M_p = full plastic moment, often termed "plastic hinge" moment.
M_{pc} = plastic hinge moment, modified to include the effect of axial compression.
M_0 = moment applied at the end of the column.
M_y = moment at which yield point is reached in flexure.
M_{yc} = same as M_y except modified for axial compression.
p = (AREA) allowable average compressive unit stress.
P = load on a column.
P_{cr} = critical load on a column. In the case of axial load alone it is P_e for buckling in the elastic range and approaches P_r in the inelastic range.
P_e = euler buckling load for a pin-ended column.
P_e' = elastic buckling load for a column pinned at one end, the other fixed.
P_e'' = elastic buckling load for a column with fixed ends.
P_L = limiting load for which the maximum moment occurs at the end of the column.
P_r = reduced or double modulus load.
P_t = tangent modulus load, the load at which bending of a perfectly straight column may commence.
P_y = axial load corresponding to yield point stress across entire section.
r = radius of gyration in the plane of bending.
r = (AREA) least radius of gyration of the member (r_1).
r_2 = (AREA) radius of gyration of the member in a direction at right angles with that of r_1.
S = section modulus.
S_e = section modulus of part of section remaining elastic.
t = flange thickness.
w = web thickness.
y = (AREA) yield point in tension.
y_0 = distance from neutral axis to centroid of section.
Z^* = plastic modulus, the static moment of the entire cross section about its neutral axis.
Z_p = plastic modulus of the plastic part of a section.
Z_e = plastic modulus of the elastic part of a section.
η = (AASHO) factor of safety based on yield point.
σ_y = lower yield point stress.
σ_{cr} = critical stress.
δ = core radius = r^2/c.
κ = eccentricity ratio = e/δ.
k = $\sqrt{P/EI}$
u = $kL/2$
ϕ = $\frac{3}{2u}\left[\frac{1}{2u} - \frac{1}{\tan 2u}\right]$
ψ = $\frac{3}{u}\left[\frac{1}{\sin 2u} - \frac{1}{2u}\right]$

(*AASHO*)

α = e_a/e_b: When e_a and e_b lie on the same side of the column axis, α is positive; when on opposite sides, α is negative; i.e., when $+e_a = +e_b$, $\alpha = +1$. $e_a = 0$, $= 0$. $+e_a = -e_b$, $\alpha = -1$.

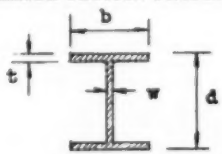
B = $\sqrt{\alpha^2 - 2\alpha \cos \phi_A + 1}$

ϕ_A = $\frac{L}{r}\sqrt{\frac{\eta f_c}{E}}$

* Z may be computed conveniently from handbook data for split T's by multiplying twice the T area by the distance from the end of the stem to the centroid of the T.

Appendix A

AVERAGE SECTION PROPERTIES



	d	b	w	t	Area	I_x	S_x	r_x
8WF31 (average of 11 specimens)								
Measured	8.069	8.032	0.299	0.427	9.177	111.356	27.60	3.483
Handbook	8.00	8.000	0.288	0.433	9.12	109.7	27.4	3.47
Variation, %	+0.86	+0.40	+3.82	−1.39	+0.63	+1.51	+0.73	+0.39
4WF13 (average of 4 specimens)								
Measured	4.139	4.140	0.258	0.337	3.759	11.215	5.419	1.727
Handbook	4.16	4.06	0.28	0.345	3.82	11.3	5.45	1.72
Variation, %	−5.05	+4.93	−7.86	−2.32	−1.59	−0.75	−0.57	+0.41
8WF40 (one specimen)								
Measured	8.300	8.070	0.371	0.550	11.691	146.82	35.379	3.544
Handbook	8.25	8.077	0.365	0.558	11.76	146.3	35.5	3.53
Variation, %	+0.61	−0.08	+1.64	−1.43	−0.59	+0.36	−0.34	+0.40

Transition from Ductile to Brittle Behavior in Pressure Vessel Steels

Discussion by Wendell P. Roop

A very great advance over earlier thought in the Pressure Vessel Research Committee is represented by the statement that though brittle failures rarely occur in pressure vessels, "nevertheless their prevention is of paramount importance." May I observe that brittle failure is relatively rare also in ships. It is fortunate that the research which it was possible to finance as a result of the ship failures could be drawn upon in the present connection.

This paper, in its function as an interpretative report, properly summarizes much that is not new, and does it in a graphic and readable way. It is no fault of the authors or sponsors that this has sometimes led to oversimplification; many ifs and buts which would be needed in a more complete treatment of the subject are here perforce omitted.

It is not my purpose to supply these omissions, but only to temper this very clear exposition with one or two caveats, and point out one or two dangerous approaches to definite error. This clarification of a complex subject is admirable, and I am sure it is intended as a clarification of a situation still fluid, and not as a last word.

As the first example I cite the statement in line 6 of the summary: "*Such* brittle failures are accompanied by but little plastic deformation...." This refers to service fractures which have often been described as completely brittle. But in the laboratory a very important intermediate case is often seen, namely, that in which a fracture of brittle appearance is preceded by large energy absorption. In fact in narrow internally notched plate specimens the energy absorbed preceding brittle fracture is no less than that preceding the inception of ductile fracture. Energy to maximum load in such specimens shows no change at the temperature of the fracture transition.

The Navy Tear Test specimen also is narrow in relation to thickness; is it fair to say that it reveals only the fracture transition? Figure 3 is incorrect when it refers to energy to maximum load in a narrow internally notched plate. Is it also incorrect when it refers to energy to maximum load in a tear test specimen?

The effect of "multidirectional" (i.e., three-directional) stresses or "notches causing restraint" has been reduced to numerical measure by Sternberg and Sadowsky,* not cited by the authors. These authors show that in the worst possible notch the flow-generating shear stress is still about four-fifths of that in the case most favorable to plastic flow.

The reference to the triaxial state of stress in pressure vessels leads me to inquire whether numerical estimates of this triaxiality can be cited.

The authors state that there is no test that can be used to select steels safe for use under predictable conditions. Should we understand that they believe such a test to be impossible?

The diagrams in Figs. 6 and 7 must be taken as schematic, perhaps in part hypothetical. They represent the idea of the two transitions but do not demonstrate it to be correct. Important new data not cited, and not clearly confirming these diagrams, are those of Harris, Rinebolt and Raring.†

Referring to the discussion of mechanical factors affecting transition temperatures, let me ask:

(*a*) Would the authors agree that the numerical degree of "multiaxiality" is almost wholly unknown?

(*b*) Is "section size" to be measured in area alone, or do width and thickness have separate and different effects?

(*c*) Do the authors entertain the hope that the very sound qualitative advice they give under "Effects of Design" can be sharpened and reduced to numbers by further study?

Such an interpretative statement as this is enormously useful, provided it is accepted on an interim basis. It is my own belief that we have not long to wait for a considerable advance in our knowledge of this subject, and that it may soon be possible to draw firmer conclusions, especially with reference to the correlation of "behavior of small-scale test specimens with the service behavior of large sections."

Authors' Reply

The authors appreciate Captain Roop's extensive comments and feel that before attempting to reply to the specific points he raises it might be in order to make a brief statement of the object of the interpretive reports sponsored by the Materials Division of the Pressure Vessel Research Committee.

Because there has been such a vast amount of research conducted on the properties of metals within the last decade, more information has been presented than has been digested and, in some instances, conflicting results have led to confusion. Also, there has been considerable duplication of effort, which at times has served no useful purpose. The attitude of the Materials Division of PVRC has been that a much more useful service could be performed by correlating and summarizing the information already available on the characteristics of materials and presenting the information in the

Wendell P. Roop, Anchorage Farm, Sewell, N. J.

Paper by T. N. Armstrong, N. A. Kahn and Helmut Thielsch was published in the August 1952 Supplement to THE WELDING JOURNAL, pp. 371-s to 380-s.

* *Jnl. Appl. Mech.*, **16,** 149-157 (June 1949).

† THE WELDING JOURNAL, Research Suppl., **XVI,** 417-s to 422-s (1951); *Ibid.*, **XVII,** 202-s to 203-s (1952).

form of interpretive reports than by initiating additional research work. The reports have been prepared with the idea in view of not only summarizing existing knowledge on a particular subject in a manner that will be understandable to the engineer who has not had specialized training in the particular field but also stimulating more fruitful research by pointing out gaps in the knowledge. These summaries cannot be considered other than progress reports as there is not even faint inference that our knowledge on any of the subjects selected is by any means complete. Perhaps there is some oversimplification but as Captain Roop states, this is unavoidable in preparing reports of this type.

Captain Roop points out that in some laboratory tests, energy absorption preceding brittle fracture may be quite large, and in certain instances may be just as great as when preceding ductile fractures. The authors agree but feel that this does not reflect on the validity of the statement in the summary: "Such brittle failures are accompanied by but little plastic deformation."

Captain Roop inquires whether it is fair to say that the Navy tear test reveals only the fracture transition. The work performed at Lehigh University for the Pressure Vessel Research Committee (9)* indicates that when the percentage of lateral contraction below the notch, for example, is used as the criterion of brittleness, the Navy tear test can be used to reveal a ductility transition.

Figure 3 is not incorrect in including "energy to maximum load" as one of the possible criteria to define transition since the diagram represents a schematic generalization of the phenomenon. It will be noted that A. Boodberg, *et al.* (Figs. 8 and 9 (54)), used energy to maximum load as a transition criterion in connection with 12-in. wide centrally notched plate specimens. In connection with the tear test, energy to maximum load is not a discretionary characteristic with respect to revealing transition and energy to propagate fracture is used as the criterion. It was not intended that all of the criteria indicated in Fig. 3 be applicable to each test method utilized for evaluating notch sensitivity of steel.

The work of Sternberg and Sadowsky, referred to by Captain Roop, appears to be limited to stress distribution around spherical and spheroidal cavities.

Some progress has been made in determining numerical estimates of triaxiality but none of the work as yet appears to be sufficiently complete for use in general design calculations.

There was no intent to convey the idea that it was impossible to develop a test that would indicate the behavior of steel as it is used in a particular structure but as yet no satisfactory test has been developed that fulfills this purpose. It was mentioned that some recently developed tests show promise.

In answer to the three questions raised in connection with the effect of mechanical factors on transition temperature:

(*a*) Certainly the numerical degree of multiaxiality is not almost wholly unknown but as yet it has not been reduced to generally accepted formulas. The work of G. Welter at École Polytechnique offers some promise of an acceptable solution as does the work of other investigators.

(*b*) Geometry as well as section area is a factor. The restraining effect of width is frequently considered to be less than that of depth in small sections.

(*c*) Considerable progress has been made in developing quantitative design data, particularly in relation to reinforced openings and external loading, in studies under the direction of the Design Division of PVRC and it is anticipated that this information will be made available in the not too distant future.

The authors agree with Captain Roop it will be possible to draw firmer conclusions as our knowledge of the subject increases. If the report does nothing else than confirm the need for further knowledge by stating only what we do know, it has served a useful purpose.

* Numbers in parentheses refer to references appended to the authors' report.

1 **BONDERIZED STEEL FRAMES,** braze welded with ANACONDA-997 Bronze Rod, make the famous "Rusco" metal combination screen and storm sash. Foreman Jack Hollister points out brazed frame corner.

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3 **SPECIAL WELDING FIXTURES,** shown in close-up, developed by "Rusco" engineers, facilitate rapid brazing of their windows. So does ANACONDA-997 Bronze Rod. It "tins" readily. The work stays clean, is easily finished.

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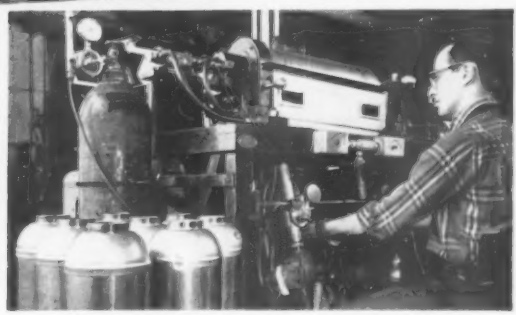
Semi-automatic Heliwelding of type 301 stainless steel is a high-speed operation at the Pyrene Manufacturing Co. of Newark, N. J. Here, jig-mounted stainless steel cylinder bodies are longitudinally weld-seamed at 90" per minute.

Finger-tip controlling two No. 10 Radiagraph arms, one operator simultaneously Heliwelds two 1000 psi pressure-tested end seams in 41 seconds.



Silent fireman with a 40-foot reach...

HELIWELDED PYRENE FIRE-STOPPER — Discharges an easily directed 40-foot stream. The new stainless steel gas-cartridge-operated Pyrene Water Type Fire Extinguisher eliminates two important fire protection problems . . . annual recharging and the danger of working with acid . . . and its price is comparable to older soda-acid equipment.

Pyrene's answer for meeting the cost problem? Lighter weight equipment, stronger-made with Airco's semi-automatic Heliwelding process.

Why? Because Heliwelding, with its inert gas shield, eliminates the need for flux. No slag is formed, welds are exceptionally strong and clean. Gas shielded electrodes provide a highly concentrated arc permitting high-economy production welding with a minimum of distortion.

If you're welding thin sections of the "problem" metals, stainless steel, aluminum, on a production basis, Airco Heliwelding can help you. Find out how from your nearest Airco office. Please address Advertising Department, 60 East 42nd Street, New York 17, N. Y. for your copy of ADC-709: Heliwelding.

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